On the integration of electromagnetic railguns with warship electric power systems

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Statement of originality

I, Ian Whitelegg confirm that the work presented in this thesis is my own. Where information has been derived from other sources, I confirm that this has been indicated in the thesis.

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DATE:

Signed:
Abstract

Electromagnetic railguns have reached levels of maturity whereby they are now being considered for installation on warships. A critical review of previous research in this field has highlighted the potential adverse impact that electromagnetic railguns may have on the supply quality of electric power systems. Currently, there is limited collective knowledge of this impact particularly when configured in a topology representative of a candidate warship.

This research explores the impact of electromagnetic railguns on a candidate warship electric power system. This research employs a validated gas turbine alternator model of the Rolls-Royce MT30 capable of assessing performance when powering an electromagnetic railgun. A novel control circuit to interface the electromagnetic railgun with the gas turbine alternator and control the rate of fire was developed. A mathematical analysis of the system was then undertaken to understand the challenges in greater detail. A system model was then developed to explore the transient and harmonic impact of electromagnetic railgun firing on the warship electric power system using time-domain simulations.

The key finding of this research is that the current practice of warship electric power system design is not robust enough to withstand electromagnetic railgun operations and that under-voltage, under-frequency, over-frequency and excessive waveform distortion result due to the high power demand of the electromagnetic railgun. To mitigate these consequences it is recommended that firing constraints be placed on the electromagnetic railgun and the maximum waveform distortion at the high voltage bus be limited to 8% total harmonic distortion. Failure to adhere to the recommended limits may result in the mal-operation, reduced efficiency and reduced life expectancy of the electric power system.
Dedications

To my parents, who believed studying electrical engineering was a good bet.
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# Nomenclature

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<tr>
<td>$T_E$</td>
<td>Exciter time constant</td>
<td>(s)</td>
</tr>
<tr>
<td>Symbol</td>
<td>Description</td>
<td>Unit</td>
</tr>
<tr>
<td>--------</td>
<td>--------------------------------------------------</td>
<td>-------</td>
</tr>
<tr>
<td>$T_F$</td>
<td>Damping filter time constant</td>
<td>(s)</td>
</tr>
<tr>
<td>$T_m$</td>
<td>Mechanical torque</td>
<td>(Nm)</td>
</tr>
<tr>
<td>$v$</td>
<td>Velocity</td>
<td>(m/s)</td>
</tr>
<tr>
<td>$V$</td>
<td>Voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_A$</td>
<td>Phase A voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_B$</td>
<td>Phase B voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_c$</td>
<td>Capacitor charge voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_C$</td>
<td>Phase C voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{Cap}$</td>
<td>ESD capacitor voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{cap, ref}$</td>
<td>ESD capacitor voltage set point</td>
<td>(V)</td>
</tr>
<tr>
<td>$v_d$</td>
<td>Volumetric displacement</td>
<td>(m$^3$)</td>
</tr>
<tr>
<td>$V_d$</td>
<td>Stator voltage direct axis component</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{DC}$</td>
<td>Mean rectifier output DC voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_h$</td>
<td>$h^{th}$ harmonic peak voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_L$</td>
<td>Line voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{max}$</td>
<td>Maximum RMS voltage during periodicity of modulation</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{min}$</td>
<td>Minimum RMS voltage during periodicity of modulation</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_n$</td>
<td>Nominal line voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_q$</td>
<td>Stator voltage quadrature axis component</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_R$</td>
<td>EM railgun rail voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{ref}$</td>
<td>Reference voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{RMAX}$</td>
<td>Voltage regulator output maximum</td>
<td>(pu)</td>
</tr>
<tr>
<td>$V_{RMIN}$</td>
<td>Voltage regulator output minimum</td>
<td>(pu)</td>
</tr>
<tr>
<td>$V_{RMS}$</td>
<td>RMS voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{shot}$</td>
<td>Capacitor voltage immediately after the shot</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{sub-transient}$</td>
<td>Synchronous alternator sub-transient voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_{transient}$</td>
<td>Synchronous alternator transient voltage</td>
<td>(V)</td>
</tr>
<tr>
<td>$V_T$</td>
<td>Synchronous alternator terminal voltage</td>
<td>(pu)</td>
</tr>
<tr>
<td>$V_{UEL}$</td>
<td>Initial value of the alternator terminal voltage</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_0$</td>
<td>Unsaturated zero sequence reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_C$</td>
<td>Capacitive reactance</td>
<td>(Ω)</td>
</tr>
<tr>
<td>$X_d$</td>
<td>Direct axis synchronous reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_d'$</td>
<td>Direct axis transient reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_d''$</td>
<td>Direct axis sub-transient reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_L$</td>
<td>Inductive reactance</td>
<td>(Ω)</td>
</tr>
<tr>
<td>$X_q$</td>
<td>Quadrature axis synchronous reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_q'$</td>
<td>Quadrature axis transient reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_q''$</td>
<td>Quadrature axis sub-transient reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$X_s$</td>
<td>Synchronous alternator stator winding reactance</td>
<td>(pu)</td>
</tr>
<tr>
<td>$Y$</td>
<td>Admittance</td>
<td>(S)</td>
</tr>
<tr>
<td>$Z$</td>
<td>Impedance</td>
<td>(Ω)</td>
</tr>
<tr>
<td>$Z_F$</td>
<td>Harmonic filter impedance</td>
<td>(Ω)</td>
</tr>
<tr>
<td>Symbol</td>
<td>Description</td>
<td>Unit</td>
</tr>
<tr>
<td>--------</td>
<td>-----------------------------------------------------------------------------</td>
<td>---------------</td>
</tr>
<tr>
<td>α</td>
<td>Firing delay angle</td>
<td>(°)</td>
</tr>
<tr>
<td>ε</td>
<td>Constant dependent upon quality and volume of magnetic material</td>
<td>( )</td>
</tr>
<tr>
<td>θ</td>
<td>Power factor angle</td>
<td>(°)</td>
</tr>
<tr>
<td>θₙ</td>
<td>hᵗʰ harmonic voltage phase</td>
<td>(°)</td>
</tr>
<tr>
<td>μ</td>
<td>Six-pulse thyristor rectifier overlap angle</td>
<td>(°)</td>
</tr>
<tr>
<td>ρ</td>
<td>Density</td>
<td>kgm⁻³</td>
</tr>
<tr>
<td>φ</td>
<td>Total power factor angle</td>
<td>(°)</td>
</tr>
<tr>
<td>φₙ</td>
<td>hᵗʰ harmonic current phase</td>
<td>(°)</td>
</tr>
<tr>
<td>Ψ⁽ᵈ and q⁾</td>
<td>Flux linkage (d and q axis)</td>
<td>( )</td>
</tr>
<tr>
<td>ω</td>
<td>Angular rotational velocity</td>
<td>rad/s</td>
</tr>
<tr>
<td>ω₀</td>
<td>Rated synchronous angular velocity</td>
<td>rad/s</td>
</tr>
<tr>
<td>ωₙ</td>
<td>Rotor angular velocity</td>
<td>rad/s</td>
</tr>
<tr>
<td>ωₛ</td>
<td>Stator current angular velocity</td>
<td>rad/s</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
<td></td>
</tr>
<tr>
<td>--------------</td>
<td>-------------</td>
<td></td>
</tr>
<tr>
<td>AC</td>
<td>Alternating Current</td>
<td></td>
</tr>
<tr>
<td>AIM</td>
<td>Advanced Induction Motor</td>
<td></td>
</tr>
<tr>
<td>AVR</td>
<td>Automatic Voltage Regulator</td>
<td></td>
</tr>
<tr>
<td>DEFSTAN</td>
<td>Defence Standard</td>
<td></td>
</tr>
<tr>
<td>DC</td>
<td>Direct Current</td>
<td></td>
</tr>
<tr>
<td>EM</td>
<td>Electromagnetic</td>
<td></td>
</tr>
<tr>
<td>EMF</td>
<td>Electromotive Force</td>
<td></td>
</tr>
<tr>
<td>ESD</td>
<td>Energy Storage Device</td>
<td></td>
</tr>
<tr>
<td>ESR</td>
<td>Equivalent Series Resistance</td>
<td></td>
</tr>
<tr>
<td>GT</td>
<td>Gas Turbine</td>
<td></td>
</tr>
<tr>
<td>GTA</td>
<td>Gas Turbine Alternator</td>
<td></td>
</tr>
<tr>
<td>GTO</td>
<td>Gate Turn-off</td>
<td></td>
</tr>
<tr>
<td>HTS</td>
<td>High Temperature Superconducting</td>
<td></td>
</tr>
<tr>
<td>HV</td>
<td>High Voltage</td>
<td></td>
</tr>
<tr>
<td>IFEP</td>
<td>Integrated Full Electric Propulsion</td>
<td></td>
</tr>
<tr>
<td>IGBT</td>
<td>Insulated Gate Bipolar Transistor</td>
<td></td>
</tr>
<tr>
<td>IGCT</td>
<td>Integrated Gate Commutated Thyristor</td>
<td></td>
</tr>
<tr>
<td>IPS</td>
<td>Integrated Power System</td>
<td></td>
</tr>
<tr>
<td>LRNS</td>
<td>Lloyd’s Rules for Naval Ships</td>
<td></td>
</tr>
<tr>
<td>LV</td>
<td>Low Voltage</td>
<td></td>
</tr>
<tr>
<td>MCR</td>
<td>Maximum Continuous Rating</td>
<td></td>
</tr>
<tr>
<td>MOD</td>
<td>Ministry of Defence</td>
<td></td>
</tr>
<tr>
<td>MOSFET</td>
<td>Metal Oxide Field Effect Transistor</td>
<td></td>
</tr>
<tr>
<td>NATO</td>
<td>North Atlantic Treaty Organisation</td>
<td></td>
</tr>
<tr>
<td>PFN</td>
<td>Pulse Forming Network</td>
<td></td>
</tr>
<tr>
<td>PI</td>
<td>Proportional Integral</td>
<td></td>
</tr>
<tr>
<td>PLL</td>
<td>Phase Locked Loop</td>
<td></td>
</tr>
<tr>
<td>PPT</td>
<td>Power Potential Transformer</td>
<td></td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
<td></td>
</tr>
<tr>
<td>--------------</td>
<td>-------------</td>
<td></td>
</tr>
<tr>
<td>PWM</td>
<td>Pulse Width Modulation</td>
<td></td>
</tr>
<tr>
<td>QPS</td>
<td>Quality of Power Supply</td>
<td></td>
</tr>
<tr>
<td>RMS</td>
<td>Root Mean Square</td>
<td></td>
</tr>
<tr>
<td>RN</td>
<td>Royal Navy</td>
<td></td>
</tr>
<tr>
<td>SMES</td>
<td>Superconducting Magnetic Energy Storage</td>
<td></td>
</tr>
<tr>
<td>STANAG</td>
<td>Standardisation Agreement</td>
<td></td>
</tr>
<tr>
<td>THD</td>
<td>Total Harmonic Distortion (up to – 3000 Hz)</td>
<td></td>
</tr>
<tr>
<td>TLC</td>
<td>Through Life Cost</td>
<td></td>
</tr>
<tr>
<td>UK</td>
<td>United Kingdom</td>
<td></td>
</tr>
<tr>
<td>US</td>
<td>United States</td>
<td></td>
</tr>
<tr>
<td>VSI</td>
<td>Voltage Source Inverter</td>
<td></td>
</tr>
<tr>
<td>ZPSU</td>
<td>Zonal Power Supply Unit</td>
<td></td>
</tr>
</tbody>
</table>
Chapter 1 Introduction

1.1 Motivation

Following the development of power electronic converters which allow the independent control of electric motor speed, from fixed speed Alternating Current (AC) generators, electric propulsion has been integrated into modern naval vessels for both operational and financial reasons (Buckley, 2002) (Ray, 2013). From an operational perspective electric propulsion inherently meets the naval requirements of increased power density, shock proof designs and low noise signatures (Buckley, 2002) (Smith, 2010). Financially, electric propulsion offers Through Life Cost (TLC) savings in terms of reduced fuel consumption and reduced maintenance costs through minimum generator operation (Hodge & Mattick, 1995, 1996, 1997, 1998, 1999, 2000). More recently, Integrated Full Electric Propulsion (IFEP), whereby common power generation is utilised for both propulsion and service loads in a power station concept (Little, et al., 2001) (Lamerton, et al., 2008) (Smith, 2010), has been adopted for front line warships. Two examples of this are the Royal Navy’s (RN) Type 45 Daring Class Destroyers (Gerrard, et al., 2007) and the United States (US) Navy’s DDG 1000 Zumwalt Class Destroyers (naval-technology.com, 2015) which is shown in Figure 1-1.

Figure 1-1 DDG 1000 Zumwalt Class Destroyer (naval-technology.com, 2015)
The power station concept employed in IFEP configurations mean that high levels of power can be generated through the parallel operation of the ship’s generators, with the previously mentioned DDG 1000 Zumwalt Class Destroyer having a total power generation capability of 82 MW (naval-technology.com, 2015). Such high levels of available power has enabled the increased electrification of weapons and sensors and future warships may field high powered Electromagnetic (EM) or directed energy weapons. Such weapons could offer advantages to warships from a personnel safety, cost and operational perspective (McNab, 2007) (Petersen, et al., 2011) (LaGrone, 2015). However, electric weapons, including technologies such as high energy lasers and EM railguns, will have a potentially significant impact on a warship’s electrical power system. This is because they demand high amounts of energy over very short periods of time, thus creating an intermittent pulsed power demand characteristic (Chaboki, et al., 2004) (Hodge, et al., 2006). Owing to this demand characteristic, electric weapons and sensors are considered pulsed loads and will intermittently load the ship’s electric power system (Sudhoff, et al., 2004) (Kanellos, et al., 2007). The power requirement of such loads may grow to represent a significant fraction of the warship’s total installed power, placing a significant burden on the generation and distribution system (Chaboki, et al., 2004). Hence the combat system may soon surpass the propulsion system in having the dominant impact on power quality on-board electric warships (Smith & Butcher, 2006). This concern is widely shared throughout the naval engineering community, with many authors including Chaboki, et al. (2004), Lewis (2006), Kanellos, et al. (2006) (2007) (2011), Steurer, et al. (2007) and Tsekouras, et al. (2010) all drawing attention to the potential impact that intermittently loading the warship’s electric power system with high power, short duration pulses may have on the Quality of Power Supply (QPS), especially to other electrical consumers when configured in IFEP architectures. As warship electrical power systems are governed by QPS standards, the adherence to which ensures compliance with and the correct operation of electrical machines and equipment connected to the supply, a complete understanding of the impact of pulsed loads on warship electric power systems is required. Potential electric weapons or pulsed type loads include EM railguns, EM armour, high energy lasers, high power microwave weapons, ultra-wideband radar and EM aircraft launch systems (van der Burgt, et al., 2004 a) (Beach & McNab, 2005) (Weise, et al., 2005 a) (Lewis, 2006) (Daw, 2015). Recently, an announcement from the US Navy that an EM railgun may feature on an IFEP warship in the near future has accelerated the requirement to understand how this particular electric weapon could be integrated into a candidate warship electric power system (Scott & Jean, 2014) (LaGrone, 2015).

However, current experimental testing of EM railguns has only been conducted at land based facilities (McNab, et al., 1995) (Wolfe, et al., 2001) when the EM railgun has been powered from an infinite power grid. While research published by the previously mentioned authors has begun to develop a general understanding around the impact of pulsed loads on warship electric power systems, validated research based specifically on the integration of EM railguns with a candidate warship electric power system is limited. Thus, a research need exists to investigate how an EM railgun could be integrated with a candidate warship electric power system. The resulting impact of EM railgun operations on QPS needs also to be explored to establish any firing constraints required to maintain an acceptable standard of QPS to other electrical consumers. Hence, this thesis aims to provide the power supply design authority with guidance
on integrating EM railguns with warship electric power systems. The objective of the guidance is to ensure that the resulting warship electric power system design will satisfy the requirements of the EM railgun and ensure that the warship’s electrical power system functions within acceptable QPS limits during EM railgun firing operations.

1.2 Research aims

The question this research aims to answer is; what are the implications and consequences of integrating an EM railgun with a warship electric power system and under what constraints should the EM railgun be operated to ensure an acceptable level of QPS to other electrical consumers during firing operations.

The research aims are summarised as follows:

1. Undertake a literature based investigation into the state-of-the-art when integrating EM railguns with warship electric power systems, examining trends in warship electric power system design and EM railguns. Provide a critical review of existing literature on integrating EM railguns with warship electric power systems to identify the key research challenges.

2. Justify the selection of, and analytically analyse, a suitably designed circuit to allow an EM railgun to be integrated with an electric power system of a candidate warship. Theoretically describe the interaction between the EM railgun and the warship electric power system to analytically and conceptually understand the impact on QPS.

3. Construct a time-domain model of the circuit under investigation which is capable of exploring the transient and harmonic impacts of EM railgun firing operations on warship electric power system QPS. Justify the selection of the model parameters and describe the model limitations. Validate and verify the model to ensure simulations yield credible results that aid the understanding of power systems performance.

4. Investigate the transient and harmonic impact of EM railgun firing operations on warship electric power system QPS through simulation based research. Explore scenarios commensurate with the combat requirements of future surface combatants likely to field EM railguns. Explain the results obtained with reference to the analytical description of the circuit under investigation presented in Chapter 3.

5. Discuss the implications and consequences of the transient and harmonic impact on QPS resulting from EM railgun operations for the design of warship electric power systems, based on the results presented in Chapter 5. Suggest approaches to mitigate any unacceptable consequences identified from a QPS perspective and discuss the merits for each case. Provide a justified and reasoned argument to support the selection of a system solution to integrate an EM railgun with a warship electric power system. Describe and justify any QPS limitations, regulations and compliance applied to the HV power system.
1.3 Thesis outline

This thesis is divided into the following 7 chapters:

Chapter 1 Introduction This chapter introduces and provides the motivation behind the research undertaken. The research aims and contributions are described and the author’s publications based on this research are presented.

Chapter 2 Literature review This chapter provides a comprehensive review of published literature in the field of EM railgun integration with warship electric power systems. This review includes establishing the state-of-the-art in warship electric power system design and a critical review into integrating EM railguns with warship electric power systems to identify the key challenges in this field.

Chapter 3 Problem formulation This chapter addresses the EM railgun concept of operation and provides a theoretic analysis of the interaction between the EM railgun and the warship’s electrical power system based on the circuit identified in Chapter 2. This chapter also describes the transient and harmonic impact of EM railgun operations on warship electric power system QPS from a mathematical perspective to understand the challenges in greater detail.

Chapter 4 Modelling Based on the mathematical analysis of the research problem presented in Chapter 3, this chapter develops the modelling tool constructed to explore the transient and harmonic impact on QPS of integrating an EM railgun with a candidate warship electric power system. This chapter also presents a thorough validation and verification of the modelling tool constructed conducted through comparison with existing experimental test data, theoretical analysis and recognised testing procedures.

Chapter 5 Results This chapter describes the results sought from this research and the investigations required to facilitate their generation using the modelling tool described in Chapter 4. This chapter then presents the results of this research and provides key observations with reference to both the theoretical analysis presented in Chapter 3 and to how the results impact upon the design of warship electric power systems.

Chapter 6 Discussion Based on the results presented in Chapter 5, this chapter offers discussion of the research results from a transient and harmonic impact perspective. The implications and consequences of EM railgun operations on warship electric power system performance are also discussed. This chapter develops the arguments which justify the selection of a final system solution to integrate an EM railgun with a warship electric power system and defines the level of QPS compliance required.

Chapter 7 Conclusions and further work This chapter presents a summary of the research findings. This chapter also details specific recommendations for the naval design authority when integrating EM railguns with warship electric power systems which have been contributed as a result of this research. Areas for further work are also suggested.
1.4 Research contributions

The author claims the following novel contributions:

1. **Proposition of an EM railgun energy storage device charging control system**
   This research has proposed and proven an elementary circuit to control the rate of charge of the EM railgun Energy Storage Device (ESD) from a Rolls-Royce MT30 Gas Turbine Alternator (GTA). The ESD charging control system has been proven capable of facilitating a maximum rate of fire of 12 shots per minute, which is commensurate with the specified requirements of future surface combatants.

2. **Development of a model with which to explore the transient and harmonic impact of an EM railgun on a candidate warship electric power system**
   This research has contributed a performance model capable of simulating the transient and harmonic impacts of EM railgun operations on a candidate warship electric power system. The novel aspect of this contribution is that the prime mover model employed in this research is based on, and validated against, the performance of the Rolls-Royce MT30 GTA. This is the first time a model of an actual GTA has been used with a system model capable of assessing the performance of the Rolls-Royce MT30 GTA when providing power for an EM railgun.

3. **Method to define the capacity of the EM railgun ESD**
   This research has contributed a method with which to define the capacity of an EM railgun ESD required to maintain the Rolls-Royce MT30 GTA load transients within acceptable limits during EM railgun firing operations. This method ensures the life expectancy and safe operation of the Rolls-Royce MT30 GTA during EM railgun operations.

4. **Recommendations on mitigating the transient impact of EM railguns on warship electric power systems**
   This research has contributed firing protocols and constraints under which the EM railgun should be operated to ensure that the transient impact on the warship electric power system remains within acceptable limits. The constraints ensure the proper operation of electrical consumers sensitive to power quality and the life expectancy and safe operation of the Rolls-Royce MT30 GTA during EM railgun operations.

5. **Recommendations on mitigating the harmonic impact of EM railguns on warship electric power systems**
   This research has contributed a recommended maximum limit for the total harmonic waveform distortion permitted at the warship electric power system High Voltage (HV) bus during EM railgun operations. Compliance with this limit ensures that the life expectancy of the GTA, transformer and power cables is not significantly reduced during EM railgun firing operations and also ensures the proper operation of electrical consumers sensitive to power quality and that the efficiency of electric power transmission is not significantly reduced during EM railgun firing operations.
1.5 Contributions to the literature

During the course of this research the following academic papers have been published or have been accepted for publication.

**Journal papers:**


**Conference papers:**


Chapter 2 Literature review

2.1 Introduction

This literature review draws on three key themes. These themes are the design of warship electric power systems, EM railguns and the integration of the latter with the former. As an understanding of each of these key themes is required, this chapter aims to furnish the reader with background in each of the areas defined. This chapter presents the literature review in three parts.

1. The aim of the first part is to provide a review of electric warships intended to develop an understanding of how and why electric power and propulsion is being integrated into modern warships. This first part also aims to inform the reader as to the state-of-the-art in warship electric power system technology.

2. The aim of the second part is to offer an overview of EM railguns, the intention of which is to provide the reader with an appreciation of the technology under investigation in this research.

3. The aim of the third part is to provide a critical review of previously published work in this field of research to identify the key challenges and areas in which knowledge is lacking. The intention of this is to justify further research in this field.

2.2 A review of electric warships

The term electric warship, defined by Hodge & Mattick (1995), describes a warship employing electric propulsion, whereby prime movers generate electricity to drive electric propulsion motors, in contrast to driving the propeller either directly or through a gearbox. In electric warships the prime movers are usually rotating machines coupled to synchronous alternators, which provide fixed frequency fixed voltage AC power (McCoy, 2002). Electric propulsion can be split into two broad categories, hybrid electric and IFEP, both of which are described by Little, et al., (2001) and Daffey & Hodge (2004). According to Little, et al.,
(2001) a hybrid electric system combines both electrical and mechanical drive systems whereas an IFEP system employs purely electric propulsion, utilising common power generation for both propulsion and service loads. The all-electric warship advances the IFEP concept further integrating high power weapons and sensors into the warship’s electric power and propulsion system (Petersen, et al., 2011).

2.2.1 A historical review of electric warships

The integration of electric propulsion into ships began following the power electronics boom of the 1980-90s. Thyristors capable of handling high voltages and currents gave rise to the development of power electronic converters, allowing the independent control of motor speed from common fixed speed AC generators. This technology was then adopted for use in the marine sector, which revolutionised the design of electrically propelled ships (Ray, 2013). There was good reason both operationally and financially to integrate electric propulsion into warships. From an operational perspective electric propulsion inherently meets the naval requirements of increased power density, shock proof designs and low noise signatures (Buckley, 2002). According to Hodge & Mattick (2008), the inherent operational advantages of electric propulsion systems were first demonstrated to the RN in the 1990s through the anti-submarine warfare Type 23 Frigate, with the requirement for a low noise signature being successfully met through the low noise levels offered by electric propulsion (Smith, 2010). While electric propulsion had been selected for previous ships and submarines the Type 23 Frigate was one of the first surface warships to employ modern power electronic converters to control the electric propulsion motors (Hodge & Mattick, 2008). Although the vessels employ a hybrid combined diesel electric and Gas Turbine (GT) solution they were the first RN warships to operate generators in parallel, paving the way for much higher powered electric propulsion systems to be installed on future classes (Hodge & Mattick, 2008) (Smith, 2010). Financially, electric propulsion offers TLC savings in terms of fuel consumption and reduced maintenance costs through minimum generator operation, an argument extensively made by Hodge & Mattick (1995, 1996, 1997, 1998, 1999, 2000), which is advantageous during times of tightening defence budgets.

More recently and in a world first for a front line warship, IFEP was selected for the RN’s Type 45 Daring Class Destroyers the first in class of which HMS Daring was launched in 2006 (Gerrard, et al., 2007). In January 2003 Thales and BAE joined forces to become the Aircraft Carrier Alliance team in order to progress Thales’ original IFEP design for the RN’s future aircraft carrier. Due to enter service in 2016 HMS Queen Elizabeth will be the world’s first IFEP aircraft carrier (Webster & Smith, 2007). Perhaps most recently, IFEP has been selected for the US Navy’s DDG 1000 Zumwalt Class Destroyers, the first in class of which was delivered in 2014 (naval-technology.com, 2015). The IFEP solution adopted for the US Navy’s DDG 1000 Zumwalt Class Destroyers has been termed the Integrated Power System (IPS), the aim of which is to provide total ship electric power to weapons, sensors and propulsion (Petersen, et al., 2011).

2.2.2 The state-of-the-art in warship electric power system technology

For the purposes of this review the warship electric power and propulsion system has been divided as according to McCoy (2002) into generation, distribution, transmission and load. As described by McCoy (2002) the generation element usually consists of a prime mover providing rotary motion coupled to a
wound field synchronous alternator. The generated power is transmitted to the connected consumers via the distribution system, with transformers being employed for Low Voltage (LV) loads such as the ship’s services. In the case of propulsion a frequency converter is required to vary the speed of the propulsion motors. This is usually facilitated by a variable speed drive, which converts the constant voltage and frequency from the distribution system, into the variable voltage and frequency required to control the propulsion motor. The state-of-the-art in the four elements described here are discussed in the following sections.

Generation
McCoy (2002) and Krolick & Amy (2007) both argue that diesel engines or GTs coupled to wound field synchronous alternators are the default options for shipboard power generation. This argument is supported by considering the generators currently installed on electric warships and those selected for future platforms. The RN Type 45 Destroyer is fitted with two 21 MW Rolls-Royce WR-21 GTAs and two 2 MW diesel generators (Gerrard, et al., 2007). Four diesel generators and two Rolls-Royce MT30 GTAs have been selected for the RN’s future aircraft carrier (Webster & Smith, 2007) and two Rolls-Royce MT30 GTAs, alongside two auxiliary GTAs have been selected to provide power for the US Navy’s Zumwalt Class Destroyer (naval-technology.com, 2015).

While the choice appears limited, Krolick & Amy (2007) argue that future power density improvements in GTAs may be achieved by allowing the turbine to turn the alternator at a speed more suited to the engine, yielding a high speed, high frequency GTA. The authors also state that future power density improvements could be achieved through technology integration such as water cooling, permanent magnets and High Temperature Superconductivity (HTS). Also, the Rolls-Royce WR-21 engine incorporates enhancements such as compressor intercooling, exhaust energy recovery and airflow management to optimise fuel economy across the operational profile (Gerrard, et al., 2007). Doerry, et al., (2010) describe the U.S. Navy efforts to increase the efficiency of their GTs, which include changing from analogue to digital fuel controls, modifying exhaust ducts and integrating an automated jet wash, enabling more frequent cleaning of the turbine without interrupting operations.

Distribution
There are four major distribution topologies in contention for employment on electric ships which are split, ring, radial and zonal, all of which are described in detail by Buckley & Crane (2007) and Schuddebeurs, et al., (2007). A simplified diagram of each distribution architecture is shown in Figure 2-1. In a split distribution system power is generated separately for propulsion and service loads. Due to their low levels of redundancy, split distribution systems have been superseded for employment on warships. Ring distribution is more commonly found on offshore support vessels employing dynamic positioning systems. Further reading on split and ring distribution systems is provided by Schuddebeurs, et al., (2007). However, as radial distribution systems are currently more widely used on warships (Ritchie, et al., 2011) neither split or ring systems are discussed further in this thesis.
In a radial distribution system power is cascaded down from the generators through switchboards to supply various consumers such as propulsion and service loads. The first level or main switchboard is usually separated by a bus tie to provide redundancy. Radial distribution systems are currently the most common choice for warships with the RN’s Type 45 Destroyer and Queen Elizabeth Aircraft Carrier both employing radial distribution systems (Gerrard, et al., 2007) (Webster & Smith, 2007). Looking to the future, zonal power systems which split the vessel into transverse power zones to align with watertight bulkheads are likely to be employed. Each zone contains a Zonal Power Supply Unit (ZPSU) which converts and distributes power into the various forms required by the loads in the zone. One of the main advantages of zonal distribution systems is their high levels of redundancy, making them particularly attractive to warships (Buckley & Crane, 2007) (Schuddebeurs, et al., 2007).

Current warship electric power distribution systems are predominantly AC (Ritchie, et al., 2011), however the potential advantages of Direct Current (DC) mean it is attracting much attention, with Blakey, et al., (2003) claiming that DC distribution was practical and achievable for all sizes of current and future platforms in 2003. A significant advantage of DC distribution to warships is that DC systems may offer space and weight savings over their AC equivalents (Butcher, et al., 2009). A current disadvantage of DC is protection, in that DC current does not benefit from the natural zero crossing point of AC current, making faults more difficult to interrupt (Hammerstrom, 2007) (Butcher, et al., 2009). However, Butcher, et al., (2009) go on to argue that modern DC architecture has overcome the protection issues of classical DC systems through protection schemes such as foldback. A further disadvantage perceived by Hodge & Mattick (2008), is that DC distribution systems may suffer potential issues with stability when supplying...
constant power loads. This argument is supported by Butcher, et al., (2009) who agree that this issue requires careful attention and that achieving stability may be at the cost of over sizing passive components. Hence, DC may be the preferred distribution medium for electric warships. For the most part however, current warship electric power distribution systems are predominantly AC (Ritchie, et al., 2011).

**Transmission**

The transmission system comprises the machinery and equipment required to convert and distribute the power generated by the ship’s generation elements to the various forms required by the loads. The various generation, transmission and load elements are connected via power cables, switchboards and circuit breakers, as shown in Figure 2-2 for a radial distribution system.

For the case of the service load, transformers are required to step down the main bus voltage, which in warships is commonly 4160 V or 11 kV, to the service load voltage which is commonly 440 V (Gerrard, et al., 2007) (Webster & Smith, 2007). The transmission system is also responsible for controlling the speed, torque and acceleration of the propulsion motor. In the case of the commonly used variable speed drive, as shown in Figure 2-2, this is achieved through switching the fixed frequency AC supply using power electronic devices to re-construct the waveform characteristics as required by the motor. This process is described in detail by Kazmierkowski, et al., (2011). It is useful to consider here that the technologies and surrounding issues discussed in this section may also apply to electric weapons if a power electronic converter were required to re-construct the fixed frequency AC supply to a form required by an electric weapon.
According to Hodge (2002) the design of power electronic converters is a trade-off between waveform quality, governed by switching frequency; size, governed by components and filters; and efficiency, governed by loss. The efficiency of propulsion converters is a measure of output power compared to input power, the difference in which is the converters loss. Losses are generated in the converter’s power electronic switching devices and manifest through heat following switching or conduction. Generally, the higher the switching frequency the better quality the waveform, therefore the harmonics are reduced. However, at high switching frequencies the switching losses of the devices begin to dominate, thus increasing the cooling requirements and decreasing efficiency. At low switching frequencies the device losses lessen thus increasing the efficiency, however the waveform quality worsens and larger filters are required. The design balance therefore, is between physical size, efficiency and waveform quality. This design trade-off applies to all power electronic converters and is not exclusive to propulsion converters.

Both Krolick & Amy (2007) and McCoy & Amy (2009) agree that early propulsion converters such as cyclo converters and load commutated inverters suffered intrinsic limitations in terms of output frequency and waveform distortion and have been superseded by Pulse Width Modulated (PWM) based converters, such as the Voltage Source Inverter (VSI). Such converters have enabled high powered AC motors to be efficiently and accurately controlled. PWM converters have a superior harmonic performance when compared with thyristor rectifiers and are often considered the best solution for marine power conversion applications due to their ability to draw near sinusoidal current from the GTA (Evans & Hoevenaars, 2007). Looking to the future, emerging converter topologies including matrix converters, described by Bucknall & Ciaramella (2007 b) and resonant converters, described by Kuznetsov (2011) aim to reduce loss and improve waveform quality.

With regards to the power electronic devices which perform the converter switching operations there are a variety of types across a range of power ratings. While an in depth description of several power electronic devices commonly used in power electronic converters is offered by Rashid (2007) and Bradley (2009 b) a summary is provided here by means of Figure 2-3 and Table 2-1. Figure 2-3 and Table 2-1 provide a summary of the thyristor, the Integrated Gate Commutated Thyristor (IGCT), the Gate Turn-off (GTO), the Insulated Gate Bipolar Transistor (IGBT) and the Metal Oxide Field Effect Transistor (MOSFET). As shown in Figure 2-3 the thyristor and the IGCT have the highest forward blocking voltage and current rating, followed by the GTO, the IGBT then lastly, the MOSFET.

As summarised in Table 2-1, which provides more precise information on the device ratings, the thyristor and the IGCT have the highest power rating, being in the 100’s of MW. The thyristor also has the lowest switching loss and on-state loss. Comparing the thyristor with the IGBT it can be seen that the power and current rating of the IGBT is an order of magnitude less. This means that to achieve a converter of equal rating, multiple IGBTs must be connected in parallel to match the power and current rating of an equivalent thyristor. This increases the device count of the converter, thus increasing its complexity and arguably reducing its reliability (Evans & Hoevenaars, 2007). Furthermore, the switching and on-state losses of IGBTs are comparatively greater when compared with thyristors which makes for a less efficient means of power conversion. However, the rated switching frequency of an IGBT is much greater than for a thyristor meaning that IGBT based converters can construct near sinusoidal waveforms, thus drawing near sinusoidal
current from the supply. This reduces harmonic waveform distortion (Evans & Hoevenaars, 2007). When selecting a switching device for a particular power electronic converter the device characteristics should be considered against the application requirements.

![Figure 2-3 Power electronic device voltage and current rating (Bradley, 2009 b) (ABB, 2015)](image)

<table>
<thead>
<tr>
<th>Performance parameter</th>
<th>Thyristor</th>
<th>IGCT</th>
<th>GTO</th>
<th>IGBT</th>
<th>MOSFET</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated power</td>
<td>100’s MW</td>
<td>100’s MW</td>
<td>10’s MW</td>
<td>100’s KW</td>
<td>100 kW</td>
</tr>
<tr>
<td>Current rating</td>
<td>3.5 kA</td>
<td>3.5 kA</td>
<td>3 kA</td>
<td>400 A</td>
<td>50 A</td>
</tr>
<tr>
<td>Voltage rating</td>
<td>6 kV</td>
<td>6 kV</td>
<td>4.5 kV</td>
<td>1.2 kV</td>
<td>500 V</td>
</tr>
<tr>
<td>Rated frequency</td>
<td>500 Hz</td>
<td>500 Hz</td>
<td>2 kHz</td>
<td>20 kHz</td>
<td>1 MHz</td>
</tr>
<tr>
<td>Switching loss</td>
<td>Low</td>
<td>High</td>
<td>Low</td>
<td>Very high</td>
<td>Very high</td>
</tr>
<tr>
<td>On-state loss</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>High</td>
<td>Low</td>
</tr>
</tbody>
</table>

While harmonic waveform distortion resulting from power electronic converters is discussed in greater detail in section 3.4.1 mitigation methods are discussed here. Without sufficient mitigation harmonic waveform distortion can cause the increased heating of electrical machines due to increased copper and iron losses which can decrease efficiency and life expectancy (Wakileh, 2001 a) (Hodge, 2002) (Bucknall, 2007 a) (Prousalidis & Kourtesis 2013 b). Furthermore harmonic waveform distortion may interference with protection systems by triggering over-current or over-voltage relays (Bayliss, 1999) and may cause the mal operation of control and communications systems which are sensitive to power quality due to higher than expected peak voltage and current waveforms (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007).

Harmonic filters are commonly employed to reduce the harmonic waveform distortion resulting from power electronic converters (Hodge, 2002) (Evans & Hoevenaars, 2007). The aim of harmonic filters is to
attenuate harmonics through resonant circuits connected in parallel with the AC power system. These resonant circuits consist of resistors, inductors and capacitors and are tuned at the targeted harmonic frequencies to provide a low impedance, high admittance path for harmonic currents (Halpin & Card, 2007). A circuit diagram for a single tuned harmonic filter is shown in Figure 2-4.

![Resonant Circuit Diagram](image)

**Figure 2-4 Per-phase harmonic filter arrangement – Single tuned**

An example of a harmonic filter frequency response is shown in Figure 2-5. Figure 2-5 shows the filter admittance plotted against frequency. For the case of this example the filter is tuned to attenuate the 5th harmonic hence presents a high admittance at 300 Hz for a 60 Hz system. This provides a high admittance path for the 5th harmonic and helps to prevent propagation into the AC power system. A harmonic filter with a lower resistance will have a sharper response and a narrower bandwidth which can be tuned in line with the requirement for harmonic attenuation (Wakileh, 2001 b).

![Harmonic Filter Response](image)

**Figure 2-5 Harmonic filter admittance against frequency**

A further advantage of harmonic filters is that the capacitive element of the filter can also provide reactive power compensation by providing reactive power to the load, as opposed to it being drawn from the generator which adversely impacts on the power factor (Kundur, 1994 a) (Kundur, 1994 d). The concept of power factor in the context of this research will be discussed further in section 3.4.1.
A simple method of providing harmonic attenuation is to use a higher pulse number rectifier. For example, as will be explained further in section 3.4.1 a thyristor rectifier comprising six thyristors is commonly referred to as a six-pulse rectifier. The predominant harmonics generated by a six-pulse rectifier are at the 5th and 7th harmonic frequencies, as will be discussed in section 3.4.1. However, if a twelve-pulse thyristor rectifier were employed the dominant harmonics would be 11th and 13th, which are lower in magnitude and can be more easily attenuated. Cancellation of the 5th and 7th harmonics is achieved by the use of a phase shifting transformer. Phase shifting transformers apply a 30° phase shift between the secondary windings of a dual wound transformer, thus the 5th and 7th harmonics cancel in the transformer secondary windings and do not penetrate into the AC network. This is because the resulting 5th and 7th harmonics are in anti-phase (Dixon, 2007). One six-pulse rectifier is then connected to each secondary winding of the transformer and their DC outputs connected in series to provide the rectifier DC output, as shown in Figure 2-6.

![Diagram of twelve-pulse thyristor rectifier](image)

**Figure 2-6 Twelve-pulse thyristor rectifier schematic (Dixon, 2007)**

If the use of a twelve-pulse rectifier does not reduce the resulting harmonic waveform distortion to the desired level the pulse number can be increased further to twenty-four-pulse. In this case cancellation of the 5th, 7th, 11th and 13th harmonics is achieved by the use of a phase shifting transformer which applies a 15° phase shift between 4 secondary windings. In this case the outputs of four six-pulse rectifiers are connected in series to provide the DC output.

The disadvantage of this solution is that the required phase shifting transformers significantly increase the overall mass and volume of the electric power transmission system. Also, as two sets of thyristor power electronic switches are required, the size of the converter increases while the efficiency decreases due to the increased switching loss. This problem is exacerbated if the use of a 24 pulse rectifier is required, as 4 sets of thyristor power electronic switches are required. The size of the transformer also increases to accommodate 4 secondary windings. A further disadvantage is that while theoretically effective this solution can sometimes perform poorly due to manufacturing tolerance in the transformer windings and due to imbalance of the applied voltage, both of which can lead to the introduction of uncharacteristic harmonics and poor cancellation of the targeted harmonics (Evans & Hoevenaars, 2007).

**Load**

The load component of a warship electric power system can be considered to have 3 major parts; propulsion, ship services and the combat system. The propulsion load, usually a low speed high torque motor, drives the propeller or thrusters which propel the ship. The design of propulsion motors, as described by Hodge & Mattick (1995), is a trade-off between size and torque. This is because the magnetic flux (iron circuit)
and stator current (electrical circuit), the interaction of which produces torque due to the Lorenz force, compete for the same space in the machine which influences the volume. The aim of the trade-off is to achieve the desired power density. The current state-of-the-art in ship propulsion motors is the Advanced Induction Motor (AIM) which is currently in service on the RN Type 45 Destroyer (Gerrard, et al., 2007) and the US Navy Zumwalt Class Destroyer (naval-technology.com, 2015). The objective of the AIM design was to increase power density, increase efficiency, improve the power factor, decrease the noise and vibration and increase the airgap for a more robust motor design (Uhbi & Norton, 2003). Looking ahead, future ship propulsion motors may utilise HTS technology. The advantages of HTS motors, as described by Kalsi, et al., (2005), include high power density, achieved through the combination of very high airgap flux density and high current density air core stator windings, up to three times the torque density of conventional machines, high efficiency, low structure-borne noise and an isothermal field winding well suited to repeated load changes.

The service load may account for between 10 and 20% of the total installed power and consists of the ship’s hotel load and auxiliary systems such as navigation and communication equipment, firefighting systems, pumps, heating and fans. The combat system load consists of the ship’s weapons and sensors which are crucial in ensuring the vessel can both defend herself and carry out combat operations. According to Lewis (2006), Andrews, et al., (2010) and Petersen, et al., (2011) warship combat systems are on the cusp of dramatic change, with the authors proposing that the next generation of warships are likely to field high powered electric weapons which offer numerous advantages from a personnel safety, weapon system cost and operational perspective. As electric weapons are the focus of this research, section 2.3 will provide a more in depth review.

2.2.3 A review of QPS standards

QPS can be considered to consist of transient and waveform quality elements. The division of QPS into transient and waveform quality elements is helpful because the cause and effect of each is distinct. Electric power system transients are caused by load variations or faults and result in voltage and frequency transients, further description of which is given in section 3.3. Non-linear power converters can cause harmonic waveform distortion and notching, both of which are discussed further in section 3.4. Harmonic waveform distortion impacts on both the voltage waveform and the current waveform, however only the voltage waveform is governed by QPS standards. The elements of QPS with the corresponding cause and effects are summarised in Table 2-2.

<table>
<thead>
<tr>
<th>QPS</th>
<th>Transient</th>
<th>Waveform quality</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cause</td>
<td>Load variations</td>
<td>Non-linear power converters</td>
</tr>
<tr>
<td>Faults</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Effect</td>
<td>Voltage deviations</td>
<td>Harmonic waveform distortion</td>
</tr>
<tr>
<td>Frequency deviations</td>
<td>Notching</td>
<td></td>
</tr>
</tbody>
</table>

Each of the considered standards govern the power supply characteristics on-board naval vessels specifying limits for transient variations in voltage and frequency expressed in terms of a percentage of nominal. The standards also govern voltage waveform quality in terms of Total Harmonic Distortion (THD). The voltage harmonic waveform distortion is expressed as a percentage of the fundamental, as shown in Equation 1, the result of which is limited by the various QPS standards. The cause and effect of harmonic waveform distortion will be explained further in section 3.4.1. A summary of each of the standards is given in Table 2-3.

\[
THD = \sqrt{\frac{v_2^{\text{Peak}} + v_3^{\text{Peak}} + \ldots + v_n^{\text{Peak}}}{v_1^{\text{Peak}}} \times 100\%}
\]  

[Eq 1]

Where \( v_1 \) is the peak value of the fundamental and \( v_2, v_3, v_4, \ldots \) are the peak values of the harmonic waveforms.

When considering the applicability of the various QPS standards to this research it is important to note that both the DEFSTAN and MIL-STD-1399, which apply to warships of the RN and US Navy respectively, have been aligned where practicable with NATO STANAG 1008. Thus, as can be seen from Table 2-3 the supply characteristic limits are largely similar, the only difference being that the DEFSTAN allows for an extremely rare transient tolerance with regards to both the voltage and frequency. This intentional alignment is because the aim of NATO STANAG 1008 is to provide operational compatibility between warships of NATO navies by specifying a mutually agreed standard (NATO, 2004). LRNS limits do not strictly align with NATO STANAG 1008 but allow for greater levels of voltage and frequency deviation and a higher level of THD when compared with the other standards. Thus, compliance with NATO STANAG 1008 inadvertently ensures compliance with LRNS. Hence, assessing the impact of EM railguns against NATO STANAG 1008 makes this research relevant for all NATO navies which may consider implementing EM railguns in the future. Furthermore, compliance with NATO STANAG 1008 also ensures compliance with LRNS, DEFSTAN and MIL-STD-1399.
Table 2-3 Summary of QPS standards – 440/115 V 60 Hz supply

<table>
<thead>
<tr>
<th>Parameter</th>
<th>LRNS</th>
<th>DEFSTAN</th>
<th>NATO STANAG 1008</th>
<th>MIL-STD-1399</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Voltage</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Permanent variation</td>
<td>+6% / -10%</td>
<td>± 5%</td>
<td>± 5%</td>
<td>± 5%</td>
</tr>
<tr>
<td>Transient variation</td>
<td>± 20% - (1.5 s)</td>
<td>± 16% - (2 s)</td>
<td>± 16% - (2 s)</td>
<td>± 16% - (2 s)</td>
</tr>
<tr>
<td>Rare transient tolerance</td>
<td>-</td>
<td>+23/-20% - (2 s)</td>
<td>± 20% - (2 s)</td>
<td>± 20% - (2 s)</td>
</tr>
<tr>
<td>Extremely rare transient tolerance</td>
<td>-</td>
<td>+50/-22% - (2 s)</td>
<td>± 5% - (2 s)</td>
<td>± 5% - (2 s)</td>
</tr>
<tr>
<td><strong>Frequency</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Permanent variation</td>
<td>± 5%</td>
<td>± 3%</td>
<td>± 3%</td>
<td>± 3%</td>
</tr>
<tr>
<td>Transient variation</td>
<td>± 10% - (5 s)</td>
<td>± 4% - (2 s)</td>
<td>± 4% - (2 s)</td>
<td>± 4% - (2 s)</td>
</tr>
<tr>
<td>Rare transient tolerance</td>
<td>-</td>
<td>± 12% - (5 s)</td>
<td>± 5.5% - (2 s)</td>
<td>± 5.5% - (2 s)</td>
</tr>
<tr>
<td>Extremely rare transient tolerance</td>
<td>-</td>
<td>+ 17% - (20 s)</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td><strong>Waveform quality</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>THD</td>
<td>8% &lt; 50th</td>
<td>5%</td>
<td>5%</td>
<td>5%</td>
</tr>
<tr>
<td>Individual</td>
<td>1.5% &gt; 25th</td>
<td>3%</td>
<td>3%</td>
<td>3%</td>
</tr>
<tr>
<td>Rare transient tolerance</td>
<td>-</td>
<td>8%</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Extremely rare transient tolerance</td>
<td>-</td>
<td>15%</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

It should be considered that the QPS limits presented in Table 2-3 only specifically govern the 440 V and 115 V 60 Hz electrical supply of a warship. Furthermore, and with reference to this research, as electric weapons were not a factor when the QPS standards were defined (Smith & Butcher, 2006) they do not explicitly make provision for EM railguns. However, with regards to pulsed loads in general NATO STANAG 1008 and MIL-STD-1399 do make basic provisions. The standards stipulate that pulsed loads exceeding the limits specified by Equation 2 and Equation 3 should be avoided, less corrective action be determined through consultation with the power supply design authority, guidance on which is one of the key aims of this research.

\[
Q_{\text{pulse}} < 6.5\% \text{ of } S_{\text{supply}} \tag{E2}
\]

\[
P_{\text{pulse}} < 25\% \text{ of } S_{\text{supply}} \tag{E3}
\]

Where \(Q_{\text{pulse}}\) is the reactive power demand of the pulsed load, \(P_{\text{pulse}}\) is the real power demand of the pulsed load and \(S_{\text{supply}}\) is the rated apparent power of the supply at the occurrence of the pulse.

Furthermore, both NATO STANAG 1008 and MIL-STD-1399 specify limits for voltage and frequency modulation, which are defined as the periodic variation of the supply voltage and frequency caused by regularly or repeated pulsed loading. The equations for calculating voltage and frequency modulation, as defined by NATO STANAG 1008 and MIL-STD-1399, are given as Equation 4 and Equation 5 respectively.

\[
\text{Voltage modulation (\%)} = \frac{(V_{\text{max}} - V_{\text{min}}) \times 100}{2 \times V_{\text{nominal}}} \tag{E4}
\]
\[ \text{Frequency modulation (\%)} = \frac{(F_{\text{max}} - F_{\text{min}}) \times 100}{2 \times F_{\text{nominal}}} \] [E5]

Where \( V_{\text{max}} \) and \( F_{\text{max}} \) represent the maximum RMS values of the voltage and frequency during the periodicity of the modulation and \( V_{\text{min}} \) and \( F_{\text{min}} \) represent the minimum RMS values of the voltage and frequency during the periodicity of the modulation.

The limits specified for voltage modulation and frequency modulation are 2\% and 0.5\% respectively. However, the provisions made in NATO STANAG 1008 and MIL-STD-1399 for pulsed loading are not commensurate with the load characteristics of EM railguns. Firstly, as will be discussed in section 2.3, the power requirement of an EM railgun far exceeds the inequalities presented in Equation 2 and Equation 3, thus consultation with the design authority on appropriate measures to be taken must be sought if EM railgun integration is required. This is because knowledge on how to integrate such loads with a warship electric power system or what the resulting impact on QPS may be does not currently exist. Secondly, the QPS modulation limit is intended to govern transients resulting from relatively small pulsed loads that are directly connected to the power system in a manner such that the power system experiences the actual behaviour of the pulsed load itself (Kanellos, et al., 2007). However, for the case of an EM railgun the load is decoupled from the power system via an ESD and a Pulse Forming Network (PFN). Hence, the impact on QPS is as a result of the interaction between the ship’s power system and the ESD, as opposed to with the EM railgun itself. This concept will be discussed and explained further in sections 2.3 and 3.2. Also, the voltage and frequency variation permitted due to modulation is accounted for in the maximum transient tolerance in both NATO STANAG 1008 and MIL-STD-1399, thus need not be considered separately during the operation of an EM railgun. It is therefore concluded, that the provisions made for pulsed loads in NATO STANAG 1008 and MIL-STD-1399 are not designed for, therefore are not applicable to, EM railgun operations. However, when integrating an EM railgun it is necessary to design the electrical system to operate within defined standards and as no specific rules and regulations for electric weapons currently exist, NATO STANAG 1008 standard makes for a universal and acceptable starting point against which to formulate discussion.

2.3 A review of EM railguns

While the array of high powered electric weapons and sensors which may feature on future warships include EM armour, high energy lasers, high power microwave weapons and ultra-wideband radar, all of which are described by van der Burgt, et al., (2004 a), Beach & McNab (2005), Weise, et al., (2005 a), Lewis (2006) and Daw (2015), the focus of this research is on the EM railgun. The research focus has been placed on this weapon due to the recent announcement from the US Navy that an EM railgun may feature on an IFEP warship in the near future (Scott & Jean, 2014) (LaGrone, 2015). Hence, to increase the relevance and immediate usefulness to naval industry of this research, efforts will concentrate on this particular weapon. Furthermore, research to include all the possible future weapons would drastically increase the scope of this research. To provide context, an image of the EM railgun is shown in Figure 2-7.
EM railguns are designed to facilitate naval surface fire support, land strikes, ship defence and surface warfare (Office of Naval Research, 2015). With a range of over 300 km and a muzzle velocity of 2.5 km/s the EM railgun will be capable of supporting tactical air assets and providing support for forces ashore, from a range outside that of all current projectiles. The range of the projectile can be reduced from a maximum 300 km through to a minimum 100 km by adjusting the EM railgun barrel angle of elevation (Petersen, et al., 2011). The current required rate of EM railgun fire is 10 – 12 shots per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015). The EM railgun aims to achieve the previously described capability without the use of chemical propellants or explosives (McNab, 2007) (Petersen, et al., 2011). As such the resulting EM railgun round is smaller and less expensive than a missile, hence tens of missiles can be replaced with thousands of EM railgun rounds, for an equivalent size of magazine (McNab, 2007). As opposed to utilising chemical propellants or explosives an EM railgun consists of two parallel rails between which sits a conductive armature and the projectile. The rails and armature form an electric circuit through which a high current is passed. The current in the rails produces a Lorentz force which acts on the armature, driving the projectile along the rails until it is launched out of the end (Hodge, et al., 2006). The simplified equivalent circuit of the EM railgun is shown in Figure 2-8 where $R_R$ is the resistance of the rails, $L_R$ is the inductance of the rails, $V_R$ is the rail voltage and $I_R$ is the rail current.

Based on a projectile mass of 20 kg and a projectile muzzle velocity of 2.5 km/s the required muzzle energy is 63 MJ (Bernardes, et al., 2003) (Chaboki, et al., 2004) (McNab, 2007). Previous research has
demonstrated that the EM railgun has an efficiency of approximately 40 % (Bernardes, et al., 2003) thus a total stored energy of 160 MJ is required per shot. To generate the high current required to facilitate the rapid acceleration of the projectile, necessary to achieve the muzzle velocity of 2.5 km/s within a barrel length of 8 to 10 m, the 160 MJ of energy is released within 6 to 10 ms (Bernardes, et al., 2003) (Wolfe, et al., 2005) (Lewis, 2006). If the pulse time is considered to be 10 ms, then 160 MJ released in 10 ms corresponds to a 16 GW pulse of power. Due to the impedance of the ship’s power system and the power limit of current prime movers it is impractical and unrealistic to draw a 160 MJ, 10 ms pulse of energy demanded by EM railguns directly from the prime mover generator. Instead the energy for the EM railgun shot is stored in an ESD, which is charged with energy provided by the prime mover generator at a controlled rate (Chaboki, et al., 2004) (McNab, 2014). The high power, short duration pulse of power is then transmitted from the ESD to the EM railgun via a PFN which forms the pulse characteristics as required by the weapon (Petersen, et al., 2011) (McNab, 2014). This concept of operation is discussed in greater detail in section 3.2.

As the EM railgun operation with regards to the warship’s electric power system is much the focus of this research, further description on the concept of operation is offered in Chapter 3. This further discussion constitutes part of the research problem formulation. The railgun characteristics resulting from this review which form the basis of the research problem formulated in Chapter 3 are summarised in Table 2-4.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Launch mass</td>
<td>20 kg</td>
</tr>
<tr>
<td>Muzzle velocity</td>
<td>2500 m/s</td>
</tr>
<tr>
<td>Muzzle energy</td>
<td>64 MJ</td>
</tr>
<tr>
<td>Required stored energy per shot</td>
<td>160 MJ</td>
</tr>
<tr>
<td>Pulse duration</td>
<td>6 to 10 ms</td>
</tr>
<tr>
<td>Pulse power</td>
<td>16 GW</td>
</tr>
<tr>
<td>Rate of fire</td>
<td>10 – 12 shots per minute</td>
</tr>
</tbody>
</table>

### 2.3.1 Potential EM railgun ESDs

Potential EM railgun ESDs include rotating machines, capacitors, batteries and Superconducting Magnetic Energy Storage (SMES) systems (McNab, 2014). Of these potential options rotating machine and capacitor based pulsed power systems have received much focus, with the University of Texas at Austin conducting research into rotating machine based pulsed power systems (Beno, et al., 2004) (Hebner, et al., 2006) (Domaschek, et al., 2007) and the US Naval Surface Warfare Centre conducting research into capacitor based pulsed power systems (Bernardes, et al., 2003) (Wolfe, et al., 2005). This is because currently, batteries and SMES based EM railgun ESDs are not considered sufficiently mature (van der Burgt, et al., 2004 a) (McNab, 2014). Hence, rotating machine and capacitor based ESDs will be discussed further in this section. As shown in Figure 2-9 rotating machine based pulsed power systems store energy in the mass of a rotating rotor. Energy is extracted from the resulting magnetic field via the stator. Power electronic
converters are required at the input and output of the rotating machine to control the speed of rotation, thus the stored energy, and to form the pulse as required by the EM railgun respectively (Domaschk, et al., 2007).

Figure 2-9 Rotating machine based pulsed power system (Domaschk, et al., 2007)

As described by Chaboki, et al., (2004) a capacitor based pulsed power system, as shown in Figure 2-10, stores the EM railgun shot energy in the electric field across the capacitor dielectric. The capacitor bank is charged via the charging circuit then the required pulse of energy released to the EM railgun following the triggering of the firing switch. The charging circuit is required to convert the fixed frequency AC supply into the DC required to charge the capacitor bank. This is accomplished via a power electronic converter, as discussed in section 2.2.2. The required pulse shape is achieved by means of a pulse forming inductor. Capacitor based pulsed power systems are realised through a modular approach, in which a bank of parallel connected capacitor modules form the complete ESD. This allows for flexibility in the forming of the pulse shape, as the modules can be triggered at set intervals (Bernardes, et al., 2003) (Wolfe, et al., 2005). In both the rotating machine and capacitor based pulsed power systems a circuit breaker is required to isolate the pulsed power system from the ship’s power system following the completion of ESD charging. This ensures that the ship’s power system is isolated from the extremely high currents present during EM railgun firing, which have been shown to be nominally 6 MA (Bernardes, et al., 2003) (Wolfe, et al., 2005).

Figure 2-10 Capacitor based pulsed power system (Chaboki, et al., 2004)

Hebner, et al., (2006) argue that rotating machine based pulsed power systems are advantageous, in that rotating machines currently have power densities an order of magnitude greater than capacitors, with systems under development nearly two orders of magnitude higher, making this solution physically smaller and lighter than an equivalent capacitor based system. However, Chaboki, et al., (2004) disagree, suggesting that rotating machine based systems are more complex due to the power conversion stages required which reduce the overall power density of the pulsed power system. This argument is supported by McNab (2014), who suggests that the required auxiliary systems such as bearings, seals and atmosphere control systems also considerably add to the mass, volume and complexity of rotating machine based pulsed power systems. McNab (2014) also states that the high output current demand on the EM railgun converter switches, which
are required to manage multiphase currents, is a significant challenge for rotating machine based pulsed power systems. Even when considering the relatively high power and current rating of thyristors when compared with other power electronic switches, as was shown in Figure 2-3 and Table 2-1, multiple thyristors connected in parallel would be required to meet the very high power and current demand of EM railguns (Bernardes, et al., 2003) (Wolfe, et al., 2005). Chaboki, et al., (2004) go on to argue that the very high speed of rotation, which is typically 12,000 rpm (McNab, 2014), means that rotating machine based pulsed power systems are on the limits of current material strengths and therefore carry increased technical risk. Furthermore, the experimental EM railgun shots which have been fired at the Green Farm Electric Gun Research and Development Facility in San Diego, have been done so from capacitor based pulsed power systems (McNab, et al., 1995) (Wolfe, et al., 2001).

Hence, it is concluded that capacitor based pulsed power systems are currently the most technically mature and are the lowest risk option for driving EM railguns. Also, owing to the experimental research conducted by Bernardes, et al., (2003) and Wolfe, et al., (2005) there is sufficient validated characteristic information available on capacitor based pulsed power systems with which to conduct further research. It is acknowledged that the mass and volume of such systems will be significant.

2.3.2 Pulsed loads in parallel industries

Following the review of EM railgun technology the literature review was extended to examine parallel industries employing pulsed loads to assess whether cross industry knowledge transfer was possible. Following this wider search of the literature, it was found that public electrical utility providers have significant experience in managing the effects of pulsed loads, due to the operation of electric arc furnaces (Baldwin, 2004) (Dehkordi, et al., 2007) and more exotic loads such as nuclear fusion experiments (Smolleck, et al., 1991). An area of concern for the electrical utility providers was the impact that pulsed loads have on local generators. Both Baldwin (2004) and Dehkordi, et al., (2007) present results that suggest pulsed loads, such as arc furnaces, may impose excessive torques on generator shafts which could cause damage to rotating parts. Both authors suggest using energy storage to dampen the resulting load transients.

Smolleck, et al., (1991) go on to suggest that pulsed loads, in this case nuclear fusion experiments, could excite torsional modes in the utility grid generators due to sub-synchronous resonance. The author also argues that if the sub-synchronous resonant frequency coincides with any of the torsional frequencies of the generator the resulting torsional oscillations could become extremely large, resulting in generator damage or failure. While Smolleck, et al., (1991) also cites stability as a potential problem following pulse load disturbances, it was concluded that it is not an area of major concern for the utility suppliers, as the power supply grid is considered infinite. However, Smolleck, et al., (1991) goes on to warn that while the investigative procedures are largely similar across industry, the result of stability studies are highly system dependant. Hence, little cross industry knowledge transfer is possible.
2.4 A critical review of integrating EM railguns with warship power systems

In initially assessing the feasibility of integrating high energy electric weapons with existing warship electric power systems Daffey and Hodge (2004) discussed the advantages and disadvantages of various options. While no experimental results were presented, conclusions on the viability of several potential solutions were offered. With regards to integrating electric weapons with hybrid electric warships Daffey and Hodge (2004) argue that extensive modifications to the platform, auxiliary systems and system fault current level preclude the installation of an additional dedicated prime mover for most platforms. Daffey and Hodge (2004) go on to suggest that either a shaft generator may be added into the propulsion train, the vessel may be operated under mechanical propulsion only or, that regeneration may be possible by operating the propulsion motors as generators, thus absorbing power from the mechanical propulsion train. It was concluded however, that all three of these options have limitations rendering them of low feasibility.

Firstly, each of the previously mentioned options are applicable only to vessels employing mechanical propulsion in hybrid power system configurations, thus ruling out candidate warships such as the RN Type 45 Daring Class Destroyer and the US Navy DDG1000 Zumwalt Class Destroyer, both of which are IFEP warships (Gerrard, et al., 2007) (naval-technology.com, 2015). Daffey and Hodge (2004) also conclude that each of these solutions would only approximately yield an additional 4 MW of power, which would limit the choice of high energy weapons available to the warship. Hence, this initial discussion concluded that due to the comparatively low levels of electric power available, it may not be possible to integrate an EM railgun with a hybrid electric warship. Following this Daffey and Hodge (2004) focus on exploiting the prime mover generators on-board IFEP warships, which generate high levels of electrical power, such as the Rolls-Royce MT30 GTA installed on the DDG1000 Zumwalt Class Destroyer. Daffey and Hodge (2004) argue that all prime movers can be overloaded to approximately 110% for short periods of time which may be exploited during the operation of high energy electric weapons. Furthermore, the authors argue that an IFEP solution allows propulsion power to be readily traded for high energy weapon power. Hence, it is concluded that IFEP warships provide the most appropriate platform for the integration of high energy weapons.

Following on from Daffey and Hodge (2004) and focusing exclusively on the integration of an EM railgun with an IFEP warship, Chaboki, et al., (2004) suggested two candidate power system configurations potentially capable of providing power for an EM railgun. The suggested configurations were parallel and open bus, based on a radial distribution system comprising two 4 MW GTAs and two 36 MW GTAs. While both configurations are plausible, the potential increase in system fault level resulting from operating such high powered GTAs in parallel suggests that the open bus arrangement would be more practically realisable when the propulsion system and the high energy weapon are in use simultaneously. This argument is supported by Daffey and Hodge (2004) who state that additional fault current limiting devices, such as a HTS fault current limiter, may have to be installed if parallel operation of the GTAs is required. While such devices are not yet mature enough to enter service in marine environment, land based installations are beginning to emerge (Bock, et al., 2015). Furthermore, when conducting combat operations it is likely that the power system would be operated in the open bus configuration with all bus ties open. This maximises
prime mover availability and minimises the warship’s vulnerability to power loss hence making the open bus arrangement more operationally favourable. The open bus arrangement suggested by Chaboki, et al., (2004) is shown in Figure 2-11.

![Diagram showing open bus arrangement](image)

**Figure 2-11 Open bus arrangement when integrating EM railgun (Chaboki, et al., 2004)**

Chaboki, et al., (2004) suggest that based on the open bus arrangement shown in Figure 2-11 the EM railgun could fire at a maximum rate of 12 shots per minute whilst the ship maintained a speed of approximately 18 kts. Alternatively, if only one EM railgun was required one GTA could be dedicated to propulsion and the other to the EM railgun, allowing propulsion to be maintained in excess of 25 kts while a single EM railgun fires at a rate of 12 shots per minute. This concept will be considered in this research. Chaboki, et al., (2004) go on to argue that when firing at a rate of 12 shots per minute an energy storage system, which stores the energy required per EM railgun shot, should be employed to manage the power system transients resulting from EM railgun operations. It is suggested that this energy storage system be based on capacitors or rotating machines with it being demonstrated that each solution can fit within the physical constraints of a candidate warship. This work by Chaboki, et al., (2004) contributes a candidate power system arrangement when integrating an EM railgun with an IFEP warship and suggests that a rate of fire of 12 shots per minute could be achieved based on analytical analysis. However, no research based evidence is offered to demonstrate the impact of the EM railgun on the warship’s electric power system from a QPS perspective when firing at the suggested rate of 12 shots per minute. In the power system configuration suggested by Chaboki, et al., (2004) the impact on QPS would affect both the EM railgun support load and the propulsion load, shown connected to the same bus as the EM railgun in Figure 2-11. Chaboki, et al., (2004) do however acknowledge that the impact on QPS will also affect the propulsion load and the EM railgun support load during EM railgun firing operations and as such, should be investigated further.

In building on the work conducted by Chaboki, et al., (2004), Sudhoff, et al., (2004) present the results of simulation based research conducted at Purdue University into the performance analysis of pulsed loads on warship IFEP power systems. Sudhoff, et al., (2004) argue, that a pulsed load which draws so much power that a prerequisite of operation is that all generators are online is unfeasible. Sudhoff, et al., (2004) therefore
suggest that the pulsed load operational requirements should be constrained to a level whereby they can be satisfied with one generator unavailable. This is commensurate with the open bus configuration suggested by Chaboki, et al., (2004) shown in Figure 2-11, if it is assumed that only one EM railgun is required. The single line diagram of the system with which Sudhoff, et al., (2004) conducted their simulation based research is shown in Figure 2-12.

![Figure 2-12 Single line diagram of reduced power, propulsion and pulsed power test bed (Sudhoff, et al., 2004)](image)

The model presented at Figure 2-12 is validated against a reduced power generation test bed constructed at the University of Purdue to investigate issues associated with integrating pulsed loads with warship electric power systems. The test bed against which the simulation models are validated consists of a 120 kW turbine emulator, a 59 kW three phase synchronous alternator with brushless excitation system, a 37 kW induction motor with load emulator, a harmonic filter, a 15 kW ship’s service load and the pulsed load. The pulsed load has an energy storage requirement of 200 kJ. All the components are coupled to a 560 V AC main bus as shown in Figure 2-12. In addition to describing the test system Sudhoff, et al., (2004) also propose two different options to interface the pulsed load with the ship’s power system, the requirement for which is to charge the ESD, as described in section 2.3. The two options presented are a transformer line commutated six-pulse thyristor rectifier and an uncontrolled rectifier buck converter combination, the circuit diagrams for which, as suggested by Sudhoff, et al., (2004), are shown in Figure 2-13 and Figure 2-14 respectively.

![Figure 2-13 Pulsed load power converter – Transformer line commutated six-pulse thyristor rectifier (Sudhoff, et al., 2004)](image)
Figure 2-13 shows the circuit diagram of the proposed transformer line commutated six-pulse thyristor rectifier used to charge the pulsed load ESD capacitor. In this case the six-pulse thyristor rectifier is used to control the rate of charge of the ESD ($C_{store}$), as described by Sudhoff, et al., (2004).

**Figure 2-14 Pulsed load power converter – Transformer uncontrolled rectifier buck converter combination (Sudhoff, et al., 2004)**

Figure 2-14 shows the circuit diagram of the proposed uncontrolled rectifier buck converter combination used to charge the pulsed load ESD capacitor. In this case the buck converter is used to control the rate of charge of the ESD ($C_{store}$), as described by Sudhoff, et al., (2004).

In both of the EM railgun interface suggestions presented by Sudhoff, et al., (2004) the pulsed load energy required per shot is stored in a capacitor based ESD, $C_{store}$. This selection of ESD medium is commensurate with the conclusions drawn from section 2.3 of this literature review. The authors do however acknowledge that the energy required per shot could equally be stored in a rotating machine. Following the presentation of the two interface options Sudhoff, et al., (2004) investigate the impact of charging the pulsed load ESD on QPS. Both the six-pulse thyristor rectifier and the uncontrolled rectifier buck converter combination are investigated. Initially, the capacitor was charged with 200 kJ from empty as quickly as possible. Following this initial charge the capacitor was isolated from the power supply and 128 kJ was discharged to simulate the firing of a shot. This initial cycle was completed within 23 s. The capacitor was then recharged with 128 kJ within 10 s and the firing of a second shot simulated. This equates to a rate of fire of 6 shots per minute, which is less than the 12 shots per minute suggested possible by Chaboki, et al., (2004). This proved an effective method of simulating the firing of multiple shots and to explore the impact of pulsed loads on QPS thus will be replicated in this research. However only two shots were simulated, therefore it has to be assumed that the power system could maintain a shot every 10 s without any deterioration in performance. This is not demonstrated in the results presented by Sudhoff, et al., (2004).

Based on the results of the research the authors argue that the six-pulse thyristor rectifier was a simple and effective solution to implement. However, this converter was found to introduce high levels of harmonic waveform distortion into the power system. The results of this research also demonstrate that the charging of the pulsed load ESD via the six-pulse thyristor rectifier adversely impacts on the generator power factor and on the generator excitation field current, the latter of which was shown to increase beyond rated during the charging of the ESD. The authors go on to argue that although the uncontrolled rectifier buck converter
option appears more complex and expensive to implement, the additional complexity and cost may be offset by the improved harmonic performance.

Despite making significant contributions to the field the approach taken by Sudhoff, et al., (2004) does have intrinsic limitations. The scale model employed is designed to simulate the performance of an IFEP warship power system when providing power for an EM railgun. While the model has been shown to successfully highlight potential issues arising when providing power for an EM railgun, no guarantee can be provided on the performance of a full scale candidate warship electric power system when conducting EM railgun firing operations and significant challenges exist in accurately scaling the L/R ratio. Furthermore, while the impact of charging the ESD on QPS is demonstrated it is not measured or discussed against a recognised standard, hence the resulting impact is difficult to quantify. Furthermore, as acknowledged by Sudhoff, et al., (2004), the research presented here does not take into account frequency transients, which as discussed in section 2.2.3 constitute a key part of the QPS standard. Hence, an improved GTA model would be required to make a complete assessment of the impact on QPS.

Thus far, a review of previously published research conducted by Daffey and Hodge (2004), Chaboki, et al., (2004) and Sudhoff, et al., (2004) has revealed several key contributions in this field. Daffey and Hodge (2004) argue that vessels employing IFEP solutions are best suited to meeting the demands of high energy weapons; Chaboki, et al., (2004) suggest that based on an open bus arrangement and a 36 MW GTA the EM railgun could fire at a maximum rate of 12 shots per minute; Sudhoff, et al., (2004) demonstrated that while a six-pulse rectifier is a simple and effective method to control the rate of charge of the ESD the rectifier also impacts on the alternator by introducing significant harmonic waveform distortion into the warship electric power system. In keeping with the simulation based approach adopted by Sudhoff, et al., (2004), Kanellos, et al., (2006) aimed to address the impact of EM railguns on both the supply voltage and frequency at a scale commensurate with a candidate warship electric power system and against a recognised QPS standard. In their research, Kanellos, et al., (2006) retain the uncontrolled rectifier approach suggested by Sudhoff, et al., (2004), albeit with a modification. As opposed to the buck converter employed to control the rate of charge of the ESD Kanellos, et al., (2006) employ a resistor bank between the uncontrolled rectifier and the EM railgun ESD, also considered to be a capacitor. The resistor bank is used to limit the current flow between the diode rectifier and the storage capacitor thus controlling the rate of charge of the capacitor. Once the capacitor is fully charged the diode rectifier is disconnected from the warship’s power system and the EM railgun shot is fired. The circuit diagram pertaining to this EM railgun ESD charging method and firing sequence is shown in Figure 2-15.
Following the selection of an EM railgun projectile mass of 4 kg and an ESD capacitor ($C_{store}$) of 1.2 mF, Kanellos, et al., (2006) explored the impact of varying the total connected resistance of the resistor bank during the charging of the ESD on QPS. Varying the resistance of the resistor bank varies the rate of charge of the ESD. The results show that as $R$ increases the voltage and frequency modulation decreases. This demonstrates that reducing the rate of charge of the ESD reduces the impact on voltage and frequency modulation. While this method of ESD charging proved capable of maintaining the voltage modulation within the NATO STANAG 1008 2% limit the frequency modulation exceeded the 0.5% limit in all cases.

The ESD charging approach presented by Kanellos, et al., (2006) may be considered simpler to implement than the uncontrolled rectifier buck converter suggested by Sudhoff, et al., (2004) however the employment of a resistor to limit the rate of charge makes for an inefficient method of power conversion. This is due to the $I^2R$ heat loss in the resistor bank, where $I$ is the ESD charging current and $R$ the resistance of the resistor bank. While this may not have been an issue for the EM railgun system considered by Kanellos, et al., (2006) it should be noted that the projectile considered in the research discussed here was 4 kg. While it may change in the future, the EM railgun launch mass is now widely considered to be 20 kg which requires a greater launch energy for the equivalent projectile velocity. Hence, to transfer the required 160 MJ launch energy for a 20 kg projectile to the ESD in the equivalent time would require a higher ESD charging current, assuming the same rate of fire. Thus, employing a resistor bank to control the rate of charge of the ESD at shot energies commensurate with full scale EM railgun shots may not be a feasible option, due to the increased $I^2R$ heat losses in the resistor bank. Furthermore, the uncontrolled rectifier-resistor bank approach did not prove capable of maintaining the frequency transients within the limits of the QPS standard considered. This conclusion drawn from the research conducted by Kanellos, et al., (2006) is supported by Steurer, et al., (2007) who, in employing the same uncontrolled rectifier ESD charging circuit as Kanellos, et al., (2006), report on an inability to maintain the main bus frequency transients within acceptable limits as defined by MIL-STD-1399, when providing power for a high energy pulsed load. Hence, this ESD charging circuit is not appropriate when considering the energy requirements of an EM railgun.

Figure 2-15 Pulsed load power converter – uncontrolled rectifier and resistor bank combination (Kanellos, et al., 2006)
The first research to explore the integration of a full scale EM railgun with a candidate warship electric power system was that conducted by Lewis (2006), who presented the results obtained from simulation based research. The models employed in the research presented by Lewis (2006) were validated against the Electric Ship Technology Demonstrator (ESTD). ESTD is a land based warship electric power system consisting of a Rolls-Royce WR21 GTA, a 4 MW Typhoon GTA, a 20 MW AIM propulsion motor, a 6 MW load bank, a harmonic filter and a comprehensive LV system load, configured in a radial AC distribution system. A system diagram of the ESTD is shown in Figure 2-16. The aim of the ESTD is to provide a means by which to measure the transient response of a full scale warship electric power system to assess the compatibility of equipment (Lewis, 2006). Employing models validated against data from the ESTD represents a significant step towards understanding the impact of EM railgun operations on a candidate warship electric power system. The advantage of the approach adopted by Lewis (2006) is that the requirement to conduct repeated experimental based developmental research on an actual candidate warship electric power system, which can incur considerable cost, is negated.

To explore the impact of EM railgun operations on the ESTD power system Lewis (2006) adopts a method to predict and optimise the AC supply voltage and frequency transients resulting from the operation of the EM railgun. This is achieved by applying a series of load changes to models of a GTA Automatic Voltage Regulator (AVR) and speed governor, developed and validated against data obtained from the ESTD system. The function of the GTA AVR and speed governor are explained fully in sections 3.3.1 and 3.3.2 respectively. Load changes represented as a ramp of between 10% and 50% of nominal load, in 10% increments, are then applied to the GTA AVR and speed governor models and the voltage and frequency response examined. The load changes are intended to simulate the impact of supplying the EM railgun shot energy to the ESD on the GTA AVR and speed governor. This approach was adopted as the ESTD does
not comprise an actual pulsed load from which experimental results could be obtained, or against which models could be validated. Lewis (2006) concludes that to minimise the voltage transient load changes should be applied as a ramp with a rise time greater than 0.3 s. Lewis (2006) also concludes that to minimise the frequency transient load changes should be applied as a ramp with a rise time greater than 4 s. Hence, it is concluded that the frequency transients, as opposed to the voltage transients, will have the dominant impact on the constraints under which the EM railgun must be operated to maintain an acceptable standard of QPS. This conclusion is commensurate with that of Kanellos, et al., (2006) and Steurer, et al., (2007) who also found that the frequency excursions resulting from EM railgun operations dominated the impact on QPS and exceeded the allowable QPS limits.

All the previously mentioned authors agree that the voltage and frequency perturbations can be minimised by controlling the rate of charge of the ESD. However, Lewis (2006) argues that all affected ship’s systems must be able to accept the periodic frequency changes caused by the pulsed load. Lewis (2006) goes on to suggest that if the ship’s power system were able to accept AC supply frequency changes of 6% an EM railgun shot every 10 s or at a rate of 6 shots per minute should be achievable. While this suggestion agrees with the rate of fire suggested by Sudhoff, et al., it is less than the 12 shots per minute suggested possible by Chaboki, et al., (2004). Furthermore, as the approach taken by Lewis (2006) is predictive, the interface between the EM railgun and the ship’s AC power system is not considered. Hence, many of the other issues identified by Sudhoff, et al., (2004), such as the impact of harmonic waveform distortion on the alternator, are not taken into account.

A more recent and significant body of work was conducted at the Hellenic Naval Academy by Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010). Although Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010) conducted research into a relatively low powered pulsed load when compared with an EM railgun, several key contributions relevant to this research were offered. Firstly, the authors suggest a more advanced circuit with which to explore the impact of pulsed loads of QPS, the diagram for which is shown in Figure 2-17. The circuit suggested is commensurate with that suggested by Chaboki, et al., (2004), Sudhoff, et al., (2004) and Lewis (2006). It should be noted that the pulsed load considered has an apparent power of 0.1 pu to base 35 MVA, which is relatively small and more akin to the power requirement of a high energy laser (Daw, 2015) as opposed to an EM railgun. Furthermore, the pulsed load considered is connected directly to the warship’s power system as opposed to being connected via an intermediate ESD, as is the case for the EM railgun. When integrating an EM railgun with a warship electric power system it may not be possible to power both the EM railgun and the propulsion load from the same bus, as is suggested for the pulsed load considered by Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010). Similarly to the approach taken by Lewis (2006) the authors also specifically model the GTA AVR and speed governor, as is shown in Figure 2-17, to examine the transient response of the voltage and frequency respectively.
The approach taken by Kanellos, et al. (2007) (2011) and Tsekouras, et al. (2010) was to investigate the influence and significance of various electrical power system parameters on the voltage and frequency modulation resulting from the operation of the pulsed load. A summary of the author’s conclusions on the significance of the electrical power system parameters on the resulting voltage and frequency modulation are summarised in Table 2-5 for consideration in this research.

Table 2-5 Significance of electric warship power systems parameters on voltage and frequency modulation resulting from pulsed load (Tsekouras, et al., 2010)

<table>
<thead>
<tr>
<th>Warship electric power system parameter</th>
<th>Parameter significance</th>
<th>Typical values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Service load factor of generator at time of pulsed load occurrence</td>
<td>Very significant</td>
<td>0.4</td>
</tr>
<tr>
<td>Service load power factor</td>
<td>Very significant</td>
<td>0.8</td>
</tr>
<tr>
<td>HV cable length</td>
<td>Less significant</td>
<td>100 m</td>
</tr>
<tr>
<td>Pulsed load period</td>
<td>Very significant</td>
<td>0.3 s</td>
</tr>
<tr>
<td>Pulsed load duty cycle</td>
<td>Very significant</td>
<td>50 %</td>
</tr>
<tr>
<td>Generator sub-transient reactance</td>
<td>Significant</td>
<td>0.15 pu</td>
</tr>
<tr>
<td>Generator inertia</td>
<td>Less significant</td>
<td>1.5 s</td>
</tr>
<tr>
<td>Governor gain</td>
<td>Not significant</td>
<td>40</td>
</tr>
<tr>
<td>AVR gain</td>
<td>Significant</td>
<td>200</td>
</tr>
<tr>
<td>Generator field voltage output limits</td>
<td>Less significant</td>
<td>- 6 pu – 6 pu</td>
</tr>
</tbody>
</table>

As shown in Table 2-5 the authors conclude that the service load factor of the generator at the time of the pulsed load occurrence is very significant, with the authors stating that increasing the service load factor beyond 0.2 tends to significantly increase the impact of the pulsed load on QPS. The authors also comment that the power factor of the service load is also significant and should ideally be between 0.7 – 0.8 to minimise the impact of the pulsed load on QPS. The High Voltage (HV) cable length, shown in Figure 2-17
to connect the generator to the HV loads, including the pulsed load, was not found to have any practical impact on QPS. The impact of the pulsed load on QPS was found to be significantly influenced by the pulsed load period and duty cycle, which in the case of the EM railgun may place constraints on the maximum rate of fire achievable. With regards to the generator parameters the authors conclude that the generator sub-transient reactance has a very significant influence over the impact of the pulsed load on QPS and should not be excessively small. The generator inertia and the governor gain were not found to have a significant impact when values within the typical range for a large marine synchronous alternator were employed. Similarly, the authors suggest that typical values can be employed for the AVR gain and AVR field voltage output limits without any significant influence over the resulting impact on QPS.

For the purposes of comparison with the synchronous alternator model parameters selected for use in this research, the values proposed by Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010) to parametrise models of the alternator, speed governor and AVR are offered in Table 2-6 and Table 2-7 respectively. These values will be used to support and justify the selection of the parameters used in this research. Parameters identified by Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010) as having a significant influence over the impact on QPS are given in bold.

### Table 2-6 Proposed synchronous alternator model parameters

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal power (MVA)</td>
<td>35 MVA</td>
<td>Quadrature axis synchronous reactance (Xq)</td>
<td>0.475 pu</td>
</tr>
<tr>
<td>Line voltage (Vn)</td>
<td>4160 V</td>
<td>Quadrature axis sub transient reactance (X&quot;q)</td>
<td>0.15 pu</td>
</tr>
<tr>
<td>Frequency (fn)</td>
<td>60 Hz</td>
<td>Unsaturated zero sequence reactance (X0)</td>
<td>0.180 pu</td>
</tr>
<tr>
<td>Direct axis synchronous reactance (Xd)</td>
<td>1.35 pu</td>
<td>Transient open circuit time constant (T'd0)</td>
<td>1.7 s</td>
</tr>
<tr>
<td>Direct axis transient reactance (X'd)</td>
<td>0.296 pu</td>
<td>Sub transient open circuit time constant (T&quot;d0)</td>
<td>0.05 s</td>
</tr>
<tr>
<td>Direct axis sub transient reactance (X&quot;d)</td>
<td>0.15 pu</td>
<td>Inertia constant (H)</td>
<td>1.5 s</td>
</tr>
</tbody>
</table>

### Table 2-7 Proposed governor and AVR model parameters

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Governor gain (K)</td>
<td>40</td>
<td>Voltage regulator output min (V_{RMIN})</td>
<td>-6 pu</td>
</tr>
<tr>
<td>Voltage regulator gain (K_A)</td>
<td>200</td>
<td>Damping filter gain (K_F)</td>
<td>0.001</td>
</tr>
<tr>
<td>Voltage regulator time constant (T_A)</td>
<td>0.001 s</td>
<td>Damping filter time constant (T_r)</td>
<td>0.1 s</td>
</tr>
<tr>
<td>Voltage regulator output max (V_{RMAX})</td>
<td>6 pu</td>
<td>Exciter gain (K_E)</td>
<td>1</td>
</tr>
</tbody>
</table>

Research into relatively small pulsed loads which are connected directly to the warship’s electrical power system, such as those considered by Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010), has also been conducted at the French Naval Academy by Scuiller (2011) (2012). While the power of the pulsed
load considered is not representative of an EM railgun and is again more aligned to a high energy laser (Daw, 2015), contributions resulting from the researched conducted by Scuiller (2011) (2012) can be considered in this research. Scuiller (2011) (2012) furthers the approach taken by the previously discussed authors through connecting an ESD in parallel with the pulsed load in an attempt to reduce the resulting voltage and frequency modulation. The premise for this, is that the parallel connected ESD provides some of the energy required to power the pulsed load, thus reducing the burden on the generator. As per the previous authors, Scuiller (2011) (2012) selects NATO STANAG 1008 against which to consider the impact on QPS. The circuit simulated by Scuiller (2011) (2012) is shown in Figure 2-18. It should be noted that Scuiller (2011) (2012) describes the pulsed load in terms of average power, as opposed to the energy required per shot. With regards the ESD both a rotating machine and a supercapacitor are considered and are interfaced with the electric power system via a power electronic converter capable of facilitating bi-directional power flow.

Figure 2-18 Proposed warship electric power system circuit with pulsed load and energy storage compensation (Scuiller, 2011) (2012)

The approach adopted by Scuiller (2011) (2012), of installing an ESD to compensate for the pulsed load, yields promising results. For the case of both the rotating machine based ESD and the supercapacitor based ESD the frequency transients were dramatically reduced when the compensator was in use, as opposed to when not. The voltage transients were also reduced, albeit to a lesser extent. However, the power of the ESD proposed by Scuiller (2011) (2012) in obtaining such impressive results may preclude the implementation of this system to compensate for the power system transients resulting from the operation of an EM railgun. This is because the power of the ESD proposed by Scuiller (2011) (2012) matches that of the pulsed load. Thus, in this case a 200 kW ESD is suggested. As was discussed in section 2.3 the pulsed power demand of an EM railgun is 16 GW, which equates to 160 MJ released in 10 ms. This pulse of power is realised by storing the energy for the EM railgun shot in an ESD, then transmitting the high power, short duration pulse from the ESD to the EM railgun via a PFN. Owing to this method of EM railgun operation
additional compensation is not required, as the rate at which energy is transferred from the ship’s power system to the ESD has been shown to control the voltage and frequency transients (Sudhoff, et al., 2004) (Kanellos, et al., 2006) (Lewis, 2006). Hence, the solution proposed by Scuiller (2011) (2012) is not feasible when considered against the power requirements of the EM railgun, nor required due to the method of operation.

Perhaps most recently and in advancing the previously discussed work into the impact of pulsed loads which are connected directly to the ship’s power system, Gattozzi, et al., (2015) present research conducted into the provision of an ESD to provide power for a high energy laser when integrated with a warship electric power system. Gattozzi, et al., (2015) consider three different laser power levels which, it is argued, are likely to be employed within the next decade. These laser powers are 30 kW, 60 kW and 125 kW, all of which are of significantly lower power than the EM railgun considered in this research. The authors suggest that ship designers will find it difficult to justify the installation of the required power capacity to handle anticipated pulsed loads purely in prime mover form and that the provision of energy storage to support such loads seems likely. This suggestion is commensurate with the recommendations made by Chaboki, et al., (2004), Sudhoff, et al., (2004), Kanellos, et al., (2006), Lewis (2006) and Scuiller (2011) (2012). The authors investigate four mediums of energy storage being lead-acid batteries, lithium-ion batteries, rotating machines and capacitors. While the conclusions of this research focus on comparing the storage mediums when supplying power for a high energy laser and not an EM railgun the authors argue that operationally, ESDs serving to support pulsed loads function in the same manner, being connected to the ship’s power supply when the pulsed load is not in use to be charged, then disconnected from the warship’s power supply and used to power the pulsed load when required. This mode of operation also applies to the ESD and PFN method considered suitable to power the EM railgun.

2.5 Summary

This chapter has demonstrated that the integration of EM railguns with warship electric power systems is clearly a new naval engineering requirement and is therefore an important and relevant field of applied research. As described, EM railguns are designed to facilitate naval surface fire support, land strikes, ship defence and surface warfare and will also be capable of supporting tactical air assets and forces ashore, from a range outside that of all current projectiles. The EM railgun aims to achieve the previously described capability without the use of chemical propellants or explosives hence allowing tens of missiles to be replaced with thousands of EM railgun rounds. This offers numerous advantages from a personnel safety, weapon system cost and operational perspective making this research of particular interest to any naval force considering the implementation of an EM railgun with a future surface combatant.

The first part of this literature review aimed to develop a general understanding of electric warships as well as informing the reader as to the state-of-the-art in warship electric power system technology. A summary of the key technological developments in warship electric power systems is provided as follows:
1. An electric warship describes a warship employing electric propulsion, whereby prime movers generate electricity to drive electric propulsion motors, in contrast to driving the propeller either directly or through a gearbox (Hodge & Mattick, 1995) (Little, et al., 2001) (McCoy, 2002).

2. IFEP warships utilise common power generation for both propulsion and service loads and have the ability to generate high levels of power. For the case of modern IFEP warships this can be in excess of 80 MW. Diesel engines or GTs coupled to wound field synchronous alternators are the default options for IFEP warship power generation. IFEP warships predominantly employ AC radial distribution systems (Gerrard, et al., 2007) (Webster & Smith, 2007) (Petersen, et al., 2011) (Ritchie, et al., 2011) (naval-technology.com, 2015).

3. Such high levels of power available and the ability to readily trade power between connected loads has enabled the increased electrification of weapons and sensors. As such, electric warship combat systems are on the cusp of dramatic change, with it being likely that the next generation of warships are likely to field high powered electric weapons. Electric weapons demand high amounts of energy over short periods of time, thus creating an intermittent pulsed power demand characteristic. Owing to this demand characteristic electric weapons will have a potentially significant impact on a warship’s electrical power system (Chaboki, et al., 2004) (Daffey & Hodge, 2004) (Sudhoff, et al., 2004) (Kanellos, et al., 2006) (Lewis, 2006) (Kanellos, et al., 2007) (Tsekouras, et al., 2010) (Kanellos, et al., 2011) (Petersen, et al., 2011) (Scuiller, 2012).

4. While there are many high powered electric weapons and sensors which may feature on future warships the EM railgun has been under increased focus. This is due to the recent announcement from the US Navy that an EM railgun may feature on an IFEP warship in the near future. EM railguns require a 16 GW pulse of power to achieve a desired muzzle velocity of 2.5 km/s with a 20 kg projectile. To facilitate this the energy for the EM railgun shot which is provided by the ship’s prime mover generators is stored in an ESD, which is charged over a relatively long period of time. The pulse energy is then transmitted from the ESD to the EM railgun via a PFN in a typically short period of time (Bernardes, et al., 2003) (Wolfe, et al., 2005) (McNab, 2007) (Scott & Jean, 2014) (LaGrone, 2015).

5. Power electronic converters are required to convert the fixed frequency AC supply into DC, as required by the weapon. Power electronic devices traditionally employed in propulsion converter topologies may be exploited however the required rating and method of operation would differ greatly from that of a propulsion converter. Furthermore, the design trade-offs traditionally associated with propulsion converters, such as waveform quality, physical size and efficiency will apply (Hodge, 2002) (Chaboki, et al., 2004) (Sudhoff, et al., 2004) (Kanellos, et al., 2006) (McNab, 2014).

As a result of critically reviewing published research into the potentially significant impact that high powered electric weapons may have on warship electric power systems it was acknowledged that significant contributions have been made in this field. The key contributions are summarised as follows:
1. While no rules which specifically account for EM railguns currently exist NATO STANAG 1008 has been identified as a universal and acceptable starting point against which to formulate research based discussion. Assessing the impact of EM railguns on QPS against NATO STANAG 1008 makes this research relevant for all NATO navies which may consider implementing EM railguns in the future. Furthermore, compliance with NATO STANAG 1008 ensures compliance with LRNS, DEFSTAN and MIL-STD-1399 (Kanellos, et al., 2006) (Lewis, 2006) (Kanellos, et al., 2007) (Tsekouras, et al., 2010) (Kanellos, et al., 2011) (Scuiller, 2012).

2. Energy storage has been identified as a key enabling technology required to augment the ship’s power system when providing power for a high powered electric weapon such as an EM railgun. As justified in section 2.3.1 capacitor based ESDs are currently the most technically mature medium and present the lowest risk option for driving EM railguns. Also, owing to previous experimental research validated capacitor characteristic information is readily available with which to conduct further research. It is acknowledged that the mass and volume of capacitor based ESDs will be significant (McNab, et al., 1995) (Wolfe, et al., 2001) (Bernardes, et al., 2003) (Chaboki, et al., 2004) (Wolfe, et al., 2005) (Lewis, 2006) (Petersen, et al., 2011) (Scuiller, 2012) (McNab, 2014).

3. Based on a candidate IFEP warship power system operating in the open bus configuration it has been suggested that a single 36 MW GTA should be able to maintain a rate of fire of 12 EM railgun shots per minute while also providing power to a 1.75 – 2.5 MW EM railgun support load. However, analytical analysis is limited since no results on the power system performance or the impact on QPS when sustaining this rate of fire have been offered (Chaboki, et al., 2004) (Lewis, 2006).

4. Various instances have been reported whereby an uncontrolled rectifier has proved incapable of maintaining an acceptable standard of QPS when employed to charge an EM railgun ESD at a rate commensurate with EM railgun operations. However, simulation based research validated against a reduced power test bed has demonstrated that a six-pulse thyristor rectifier provides a simple and effective method of controlling the rate of charge of the ESD, hence providing a suitable interface between the EM railgun and the warship’s electric power system. Harmonic waveform distortion resulting from the six-pulse thyristor rectifier has been shown to adversely impact on the synchronous alternator however no solutions commensurate with EM railgun operations were offered (Sudhoff, et al., 2004) (Kanellos, et al., 2006) (Steurer, et al., 2007).  

5. Simulation based research validated against experimental data from the ESTD has demonstrated that to minimise voltage and frequency transients, load changes commensurate with charging an EM railgun ESD should be applied as a ramp with a rise time greater than 0.3 s and 4 s respectively. This suggests that the frequency transients, as opposed to the voltage transients, will have the dominant impact on the constraints under which the EM railgun must be operated to maintain an acceptable standard of QPS. It is also suggested that if the ship’s power system were able to accept AC supply frequency changes of 6% an EM railgun rate of fire of 6 shots per minute should be achievable. However, the interface between the EM railgun and the ship’s power system has not been considered in the model,
hence previously highlighted issues such as the impact of harmonic waveform distortion on the synchronous alternator are not taken into account (Lewis, 2006).

6. When considering pulsed loads of a relatively low power when compared with an EM railgun it has been found that various power system characteristics significantly influence the impact of pulsed load operations on QPS. These are the service load factor of the generator at the time of the pulsed load occurrence, the power factor of the service load, the pulsed load period, the pulsed load duty cycle and the generator sub-transient reactance (Kanellos, et al., 2007) (Tsekouras, et al., 2010) (Kanellos, et al., 2011).

Much of the existing work in this field has considered a relatively low powered pulsed load which is more representative of a high energy laser connected directly to the warship’s electrical power system. Where previous research has considered the integration of an EM railgun with a candidate warship electric power system the associated power electronic interfaces between the ship’s power system and the EM railgun have not been accounted for. Furthermore, continuous firing at the suggested rate of 12 shots per minute has not yet been demonstrated while maintaining an acceptable standard of QPS. Hence, two key ways in which this field of research may be advanced further are summarised as follows:

1. A validated model with which to explore the impact of EM railgun operations on a candidate warship electric power system. The model should include the ability to allow investigation into the impact of the electrical interface between the warship’s electric power system and the EM railgun on QPS.

2. The design and development of an EM railgun specific ESD and charging control system capable of facilitating continuous firing at the suggested rate of 12 shots per minute while simultaneously maintaining an acceptable standard of QPS. The operational constraints within which the EM railgun should be operated to maintain an acceptable standard of QPS should also be defined.

Lastly, a summary timeline of the key published research discussed in this section of the literature review is presented in Figure 2-19. For the purposes of completeness and to demonstrate continuity in this field of research, publications resulting from the research reported on in this thesis, presented in section 1.5, have also been included.
Timeline of key published research into integrating EM railguns with warship electric power systems

**2001**
- **F. Kanellos, et al.**
  Hellenic Naval Academy
  *Investigation of voltage/frequency modulation in ship electric networks with pulsed loads according to STANAG 1008 design constraints*

**2002**
- **A. Lewis**
  Converteam
  *Optimising the AC interface of high power pulse loads on combatants with integrated electric propulsion*

- **S. D. Sudhoff, et al.**
  Purdue University
  *Performance Analysis of Pulsed Loads on Integrated Power Systems*

**2003**
- **A. Chaboki, et al.**
  United Defense L.P., USA
  *Integration of electromagnetic railgun into future electric warships*

- **F. Kanellos, et al.**
  Hellenic Naval Academy
  *Simulation of a Shipboard Electrical Network (AES) Comprising Pulsed Loads*

- **G. Tsokouras, et al.**
  Hellenic Naval Academy
  *STANAG 1008 Design Constraints for Pulsed Loads in the Frame of the All Electric Ship Concept*

**2004**
- **F. Kanellos, et al.**
  Hellenic Naval Academy
  *An effort to formulate frequency modulation constraints in ship-electrical systems with pulsed loads*

**2005**
- **Whitelegg, et al.**
  University College London
  *On electric warship power system performance when meeting the energy requirements of electromagnetic railguns*

**2006**
- **Whitelegg, et al.**
  University College London
  *Integrating electric weapons with surface combatants - a power system performance based investigation*

**2007**
- **Whitelegg, et al.**
  University College London
  *The impact of pulse loads on electric warship power systems*

- **Daffe & Hodge**
  Rolls-Royce
  *Mid-life crisis! How to cope with new high energy systems late in life*
Chapter 3 Problem formulation

3.1 Introduction

The aim of this chapter is to develop a complete conceptual understanding of the potential impacts of EM railguns and their associated system interfaces on electric warship QPS which is required to understand the challenges in greater detail. As such this chapter provides background reading and analytical analysis into the problem posed by integrating EM railguns with warship electric power systems. This conceptual understanding of EM railgun operation on QPS should take into account single shot and continuous firing, both of which may be encountered in theatre and which would depend on the nature of the intended target (McNab, 2007). Hence, it is necessary to understand how the EM railgun interacts with a candidate warship electric power system, how such interaction would impact on QPS and what this means for the design of warship electric power systems. As such, this chapter addresses how an EM railgun could be electrically integrated into a warship electric power system and how having done so, the resulting EM railgun operation would impact on the transient and harmonic elements of QPS. Hence, the research problem is formulated as follows:

1. Describe the EM railgun concept of operation and identify the key stages during which the EM railgun will impact on the transient and harmonic elements of QPS.

2. Identify the electric power system control systems responsible for maintaining the transient elements of QPS. Mathematically explore their ability to manage the demands of providing power for the EM railgun.

3. Identify a suitable power converter to convert the power generated by the GTA into a form required by the EM railgun. Mathematically explore the impact of the electric power converter on the waveform quality element of QPS.
3.2 EM railgun system concept of operation

Figure 3-1 shows a simplified EM railgun power system diagram, commensurate with those suggested by Chaboki, et al., (2004), Lewis (2006), Kanellos, et al., (2007) (2011) and Tsekouras, et al., (2010). As was concluded from Chapter 2 this circuit is a suitable model with which to explore the impact of EM railguns on QPS. For the case of such a power system the energy required by the EM railgun is to be provided by the prime mover generator. Due to the inherent impedance of the power system and the power limits of current prime movers, it is impractical and unrealistic to draw a 160 MJ, 10 ms pulse of energy demanded by EM railguns directly from the prime mover generator (Bernardes, et al., 2003) (Lewis, 2006). Instead, an intermediate controlled charging circuit can be employed to draw and store energy provided by the prime mover generator in an ESD, prior to it being supplied to the EM railgun via a PFN. The PFN consists of a solid state thyristor switch, used for triggering the discharge of the charged ESD into the EM railgun, in series with a pulse forming inductor, used to shape the pulse. In practice a modular approach would be realised, with multiple ESD modules being triggered at set intervals to facilitate more accurate shaping of the pulse, as described by Bernardes, et al., (2003) and Wolfe, et al., (2005). The ESD is considered here to be a capacitor bank. As was concluded from section 2.3.1 of the literature review, capacitor based ESDs are currently the most technically mature and lowest risk option for driving EM railguns. Hence, a capacitive ESD is employed in this research.

![Figure 3-1 Simplified EM railgun system diagram](image)

The steady state energy transfer associated with the EM railgun system with the associated stage losses is shown in Equation 6.

\[
\dot{m}_{\text{fuel}} = \frac{1}{2}j\omega^2 - loss(GTA + Transmission + Switching) \rightarrow \frac{1}{2}CV^2 - loss(ESD) \rightarrow \frac{1}{2}LI^2 - \frac{1}{2}mv^2 - loss(PFN + EM railgun)
\]

Where \(\dot{m}_{\text{fuel}}\) is the mass flow rate of fuel; \(\frac{1}{2}j\omega^2\) is the inertial energy of the prime mover generator; \(\frac{1}{2}CV^2\) is the energy stored in the ESD; \(\frac{1}{2}LI^2\) is the electromagnetic energy in the rails of the weapon and \(\frac{1}{2}mv^2\) is the kinetic energy of the projectile.

Stored chemical energy in the fuel is converted into mechanical energy by the prime mover which is then converted into electrical energy by means of an alternator. The prime mover and alternator retain a portion of the energy as rotational energy or inertia. Electrical energy is transferred to and stored in the ESD by means of the controlled charging circuit, completing the first stage of the energy transfer process. Once the
ESD has been charged, energy can be released from the ESD via the PFN with the energy being converted into electromagnetic energy in the rails of the weapon and into kinetic energy in the projectile by means of the Lorentz force (Hodge, et al., 2006). This forms the second stage of the energy transfer process. Energy is lost in each conversion and transfer stage. Hence, the EM railgun firing can be considered a two stage energy transfer process. In the first stage energy is transferred from the GTA to the ESD. In the second stage energy is transferred from the ESD to the rails of the weapon via the PFN and ultimately to the projectile.

The operation of an EM railgun, from the perspective of the power system, can therefore be split into two distinct stages; the ESD ‘charging stage’, during which energy is transferred from the prime mover generator to the ESD, and the ‘firing stage’, during which energy is transferred from the ESD to fire the projectile. The relationship between the first and second stage is the rate of charge of the ESD. Owing to this split stage operation the charging of the ESD is decoupled from the firing of the EM railgun ensuring that the ESD cannot be connected to the GTA and the PFN at the same time. This arrangement has several advantages and disadvantages. A significant advantage is that the GTA is not required to deliver the high pulsed power demand of the EM railgun directly but instead is tasked with the controlled charging the ESD before each firing of the EM railgun. This concept has been demonstrated by Sudhoff, et al., (2004), Kanellos, et al., (2006) and Lewis (2006) and was discussed in Chapter 2. The disadvantage is that the GTA will be subjected to a significant load upset upon commencement of charging the ESD and upon decoupling from the ESD once charging is complete, with the GTA perhaps ramping up and down in power at the maximum permitted rate to charge the ESD at the maximum permitted rate. As the load changes will occur during the ESD charging stage, it is this first part of the energy transfer that is of interest, since the charging of the ESD will impact on QPS thus the design and operation of the warship’s electrical power system.

In addition to firing single shots continuous firing may be required whereby multiple shots are fired at a sustained rate, as described by McNab (2007) and Osborn (2015). As discussed in Chapter 2 it was demonstrated by Chaboki, et al., (2004) and Sudhoff, et al., (2004) that when this is the case it is desirable for the GTA to continuously charge the ESD for the duration of the continuous firing operation thereby maintaining the GTA at its maximum output power, as opposed to unloading following each shot. Under such arrangements the GTA would supply maximum rated power during charging of the ESD and when firing the EM railgun for the duration of the continuous firing operation. This concept is demonstrated in Figure 3-2, which shows the ESD capacitor energy in the upper plot and the GTA power in the lower plot. Under such conditions the GTA will be subjected to load changes on commencement of the initial charge shown in Figure 3-2 as the ESD charge time. This ESD charge time will be limited by the capability of the GTA output voltage, frequency and power. The GTA will also be subject to load changes on commencement of each subsequent recharge following each shot. This load change will depend on the characteristics of the ESD which will be described further in section 3.4.2. When firing either a single shot or firing continuous shots the resulting changes in GTA load may impact on the warship’s electrical power system. How this impact may manifest in terms of QPS will be discussed in the following section.
3.3 The impact of EM railgun operation on QPS

According to Kundur (1994 c) the quality of an electrical power supply must meet certain standards with regards to constancy of voltage and constancy of frequency. Kundur (1994 e) further describes the stability of a power system as the ability of a power system to remain in a state of operating equilibrium under normal operating conditions and to regain an acceptable state of equilibrium following a transient disturbance. Hence, the transient impact of EM railguns on warship electric power system QPS can be considered from a stability analysis perspective.

Owing to the finite nature of the power system considered in Figure 3-1, fixed reference quantities traditionally associated with stability such as synchronous speed and load angle are lacking. As such, the stability of the power system in terms of constancy of voltage and frequency is determined by the generator AVR and prime mover governor respectively. Configured as shown in Figure 3-3, the generator AVR controls the magnitude of the generator Electromotive Force (EMF) by controlling the field current, while the governor controls the speed of the prime mover by controlling the fuel input. As the performance of the AVR and governor are critical in maintaining stability at all times during EM railgun operation additional demands are placed on their function and effectiveness. The operation of the AVR and governor is discussed in greater detail in the following sections.

Figure 3-2 ESD energy and GTA power during continuous EM railgun firing (Chaboki, et al., 2004)
Problem formulation

3.3.1 Generator AVR

The AVR forms the control element of the GTA excitation system and is responsible for maintaining the alternator terminal voltage and meeting the reactive power drawn by the load. The AVR achieves both of these functions by controlling the DC field current applied to the alternator field winding. While an in depth explanation of AVRs is offered by Watson (1981 b), Kundur (1994 b) and Wildi (2006 b) this section will offer adequate reading to understand how EM railgun operations will impact on the ability of the AVR to maintain the generator voltage stability and to match the reactive power demand.

While Watson (1981 b) and Kundur (1994 b) describe many different types of generator excitation system, the configuration considered appropriate for use in this research is compound source, the circuit diagram for which is shown in Figure 3-4. This particular excitation system has been selected because the excitation system power is derived from the voltage and current of the main generator through a Power Potential Transformer (PPT) and a saturatable current transformer respectively, as shown in Figure 3-4. To control the field current the AVR controls the saturation of the current transformer by trimming the excessive excitation current down to the required value (Watson, 1981 b). Therefore, under large load disturbances after which the generator terminal voltage may be depressed, the exciter current input enables the exciter to provide a high field current, thus quickly recovering the terminal voltage (Watson, 1981 b) (Kundur, 1994 b). As such this type of excitation system has a fast acting response, which is advantageous in the absence of fixed reference quantities.
To further understand the relationship between the AVR and large synchronous alternator load disturbances consider the AVR and synchronous alternator stator quasi-steady state per phase equivalent circuit shown in Figure 3-5. For the purposes of time-domain simulations a transient Direct (d) and Quadrature (q) axis model is required, the development of which is described in section 4.4.2. Hence, the d and q notation used for the purposes of explaining the quasi-steady state per phase equivalent circuit in this section will be explained fully in section 4.4.2. However, for the purposes of this discussion a quasi-steady state simplification will suffice. The following quasi-steady analysis forms the basis for the transient d and q axis model described in section 4.4.2.

![Figure 3-5 AVR and synchronous alternator stator per phase equivalent circuit](image)

Where the notation is as follows:

\[ e_{fd} \] - excitation field voltage  
\[ i_{fd} \] - excitation field current  
\[ R_{fd} \] - excitation field resistance  
\[ L_{fd} \] - excitation field inductance  
\[ jX_s \] - alternator stator reactance (large relative to \( r_a \))  
\[ r_a \] - alternator stator resistance (small relative to \( jX_s \))  
\[ i_a \] - alternator current flow  
\[ e_a \] - alternator terminal voltage  
\[ E_a \] - alternator internal voltage (controlled by the excitation field current).

The alternator terminal voltage \( e_a \) can be expressed in terms of Equation 7, in which the variables are as per those presented in Figure 3-5.

\[ e_a = E_a - jX_s i_a - r_a i_a \]  \[\text{[E7]}\]

Under constant load and constant excitation \( E_a \) would remain constant. As such, as \( i_a \) increases under increasing alternator load \( e_a \) would decrease owing to the voltage drops across \( r_a \) at unity power factor and across \( r_a \) and \( jX_s \) at lagging power factors (Watson, 1981 a). At unity power factor all the current drawn is real, thus the voltage drop across \( jX_s \) is negated. This characteristic is known as voltage droop. To maintain
the magnitude of $e_a$ under increasing load the AVR must increase $i_{d}$ to increase $E_a$, thus balancing Equation 7. The resulting phasor diagram for lagging power factor conditions is shown in Figure 3-6.

![Figure 3-6 Synchronous alternator phasor diagram for lagging power factor conditions](image)

However, under transient events such as faults, or in the case of this research the rapid transfer of energy from the GTA to the EM railgun ESD, the response is more complex. During transients the alternator internal reactance $jX_s$ exhibits three distinct stages being the sub-transient reactance, whereby the winding reactance is at the smallest realisable value, through a transient reactance, during which the winding reactance is higher than during the sub-transient stage, through to the steady state or synchronous reactance whereby the winding reactance is at nominal value (Watson, 1981 a) (Prousalidis & Kourtesis, 2013 a).

The sub-transient reactance, represented by $X''_{d}$, comprises the reactance of the air leakage paths of the stator windings and is usually about 10% of the machine’s base reactance (Watson, 1981 a). The duration of the sub-transient reactance, denoted by $T''_{d}$, is equal to 2-5 cycles of current following the disturbance, or around 30 – 80 m/s for a 60 Hz system (Prousalidis & Kourtesis, 2013 a). During this stage the alternator terminal voltage is equal to Equation 8.

$$e_{sub-transient} = E'_q - jX''_d i_a - r_a i_a$$  \[E8\]

The transient reactance, represented by $X'_{d}$, is higher than the sub-transient reactance but still variable (Watson, 1981 a). The duration of the transient reactance, denoted by $T'_{d}$, is equal to 10-15 cycles of current, or around 166 – 250 m/s for a 60 Hz system (Prousalidis & Kourtesis, 2013 a). During this stage the alternator terminal voltage is equal to Equation 9.

$$e_{transient} = E'_q - jX'_d i_a - r_a i_a$$  \[E9\]

Following the transient stage the alternator reactance settles to steady state and the alternator voltage returns to that expressed in Equation 7. The relationship between the alternator current, voltage and the sub-transient, transient and steady state reactance is shown graphically in Figure 3-7. With reference to Figure 3-7, it is important to note that the magnitude of the voltage drop that occurs during the sub-transient stage cannot be corrected for by the AVR. The AVR can only manage the recovery time and permanent variation (Watson, 1981 a). This is an important consideration when assessing the impact of EM railguns on the warship electric power system voltage stability.
Hence, it has been shown that for the case of a finite power system, the constancy of the voltage following a large load disturbance will depend on the characteristics and response of the excitation system and AVR and, on the sub-transient, transient and steady state reactance of the synchronous alternator. As discussed in Chapter 2, Tsekouras, et al., (2010) identified the sub-transient reactance of the alternator as having a significant influence over the impact of pulsed loads on the voltage and frequency transient elements of QPS, meaning that the characteristics described in this section are of significant importance to this research.

3.3.2 Generator governor

The generator governor is responsible for controlling the generator real power output and speed. As the frequency of the generated voltage is proportional to the speed of the generator multiplied by the number of pole pairs, as shown by Equation 10, the governor controls the frequency of the generated voltage $f_n$ by adjusting the prime mover fuel flow to control the speed, $N_s$.

$$f_n = N_s \cdot p$$  \[E10\]

Where $f_n$ is the nominal system frequency; $N_s$ is the rated synchronous speed and $p$ is the generator pole pairs.
As the fuel input essentially governs the energy into the system, the mass flow rate of fuel must also meet the real power demand of the load. As such, a portion of the energy demanded by the load is retained as rotational energy, or inertia, in the prime mover generator to maintain frequency, whilst the rest is transferred to the load. As the load on the generator increases, the governor increases the fuel input to the prime mover to maintain the speed under the increased load demand, thus maintaining the system frequency. Failing to do so would result in the load extracting energy from the generator’s rotational energy, thus reducing the frequency. For a 60 Hz system and a 2 pole generator the governor would aim to maintain the prime mover speed at 60 revolutions per s, as denoted by Equation 10.

While a detailed description of various types of governor control is offered by Kundur (1994 f), this research will focus on a governor under Proportional Integral (PI) control as shown in Figure 3-8. A PI controller has been selected because it allows for a fast acting and stable response respectively, which is desirable when seeking to minimise frequency perturbations resulting from power system transients. This is as opposed to simply employing proportional control, which may yield an unstable response (Kundur, 1994 f). Similarly, the use of derivative gains can result in instability when the generator is coupled to an interconnected system (Kundur, 1994 f). As such neither are considered appropriate for use in this research. Furthermore, a PI controller was employed in the speed governor model developed by Lewis (2006), the response of which was validated against the ESTD, as described in section 2.4. As shown in Figure 3-8, the generator speed feedback is subtracted from the speed reference signal to create an error signal. This is then processed through the PI controller and used to adjust the fuel flow actuator.

![Figure 3-8 PI control prime mover governor system diagram](image)

### 3.4 Electrically integrating EM railguns with warship electric power systems

Having offered an explanation as to the concept of operation of EM railguns, their impact on warship electric power systems and on the key generator control components responsible for maintaining the voltage and frequency components of QPS during EM railgun operation, this section will suggest how an EM railgun could be electrically integrated into a warship electric power system. Furthermore this section will take an analytical approach towards defining the characteristics of a warship electric power system comprising an EM railgun.

Consider the warship electric power system represented at Figure 3-9, the characteristics of which have been selected based upon the power and propulsive requirements of a candidate warship (LaGrone, 2015) (naval-technology.com, 2015). The system incorporates an AC power generation capability comprising 2
Problem formulation

x 36 MW GTAs and 2 x 5 MW GTAs which can be connected to a common bus. Transformers step-down the main bus voltage from 11 kV to 440 V for the hotel and service loads. With a total installed power of 82 MW the vessel is able to achieve in excess of 30 kts (naval-technology.com, 2015) whilst maintaining hotel and service loads totalling 4 MW in the Action State 1 (four island) configuration with prime mover availability maximised. As was concluded in Chapter 2, this radial four island distribution configuration is typical of an electric warship (Gerrard, et al., 2007).

![Diagram of electrical power system](image)

**Figure 3-9 Candidate warship electric power system under investigation**

Integrated into the candidate warship electrical power system is an EM railgun with its associated ESD charging system. As discussed in Chapter 2, the parameters of the EM railgun have been derived from information provided by Wolfe, et al., (2005) Hodge, et al., (2006) Lewis (2006) and Petersen, et al., (2011), which all offer correlating specifications that are summarised below in Table 3-1. The ESD can be charged from either the port or starboard HV propulsion switchboard thereby providing a level of redundancy. Also shown in Figure 3-9 is the 2 MW EM railgun support load, as described by Chaboki, et al., (2004). The EM railgun support load consists of the power required by the mount to train the direction and elevation of the barrel, the magazine and the cooling load, pertaining specifically to the EM railgun. Guaranteeing that an acceptable level of QPS is delivered to the EM railgun support loads during EM railgun firing operations will help to ensure proper and continuous operation and thus, facilitate the required support of the EM railgun. As the EM railgun and the EM railgun support load are co-dependant, powering them from the same bus as shown in Figure 3-9 is considered reasonable and supported by Chaboki, et al., (2004).
Table 3-1 Railgun parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Launch mass</td>
<td>20 kg</td>
</tr>
<tr>
<td>Muzzle velocity</td>
<td>2500 m/s</td>
</tr>
<tr>
<td>Muzzle energy</td>
<td>64 MJ</td>
</tr>
<tr>
<td>Required stored energy per shot</td>
<td>160 MJ</td>
</tr>
</tbody>
</table>

When the warship’s power system is configured as shown in Figure 3-9 each prime mover generator is supplying its own independent electrical distribution system. The 5 MW GTAs will supply the ship’s LV service load, one 36 MW GTA will provide power for propulsion whilst the other 36 MW GTA will provide power for the EM railgun and the EM railgun support load.

When considering the first part of the energy transfer process, being the charging of the ESD, the characteristics of the controlled rectifier charging bridge, shown in Figure 3-9, are of utmost importance regards the impact on QPS. The rectifier considered to regulate the ESD charge cycle in this research is the six-pulse fully controlled thyristor bridge. As was concluded from Chapter 2, simulation based research validated against a reduced power test bed has demonstrated that a six-pulse thyristor rectifier provides a simple and effective method of controlling the rate of charge of the ESD. This type of rectifier can be connected directly to a warship’s three phase electrical power system without the need for bulky phase shifting transformers. This is a configuration previously used in warships, exemplified by the RN Type 45 Destroyer and the Type 23 Frigate, both of which employ six-pulse thyristor rectifiers connected directly to the three phase electrical power system, albeit to control the propulsion motors as opposed to charging an EM railgun ESD (Hodge & Mattick, 2008) (Gates, 2014).

As opposed to opting for a twelve-pulse rectifier to reduce the harmonic waveform distortion resulting from the six-pulse rectifier the Type 45 Destroyer employs harmonic filters, which were discussed previously in section 2.2.2, on both the HV and the LV switchboards (Gerrard, et al., 2007) (Gates, 2014). This is because the power density of the twelve-pulse rectifier solution was not considered commensurate with the physical constraints of a warship (Gerrard, et al., 2007). As shown in Figure 3-9 a harmonic filter is not initially considered in this research. This is because before an assessment of the harmonic waveform distortion resulting from using the six-pulse thyristor to charge the EM railgun ESD is made, the harmonic filter requirements cannot be properly understood. It is acknowledged that the six-pulse fully controlled rectifier will introduce harmonic waveform distortion on the AC supply side during the charging of the ESD, in particular 5th and 7th harmonics at 20% and 14% of the fundamental respectively. The resulting harmonic waveform distortion is discussed further in section 3.4.1 and is explored in Chapter 5.

For the case of this research and as per the circuit suggested by Sudhoff, et al. (2004), an isolation transformer has been employed. While it is acknowledged that the isolation transformer would decrease the overall power density of the EM railgun system, it is considered a simple and robust method of achieving harmonic attenuation. This is because delta connected transformers prevent the flow of zero sequence harmonics thus serve to protect the load and source side of the power system by acting as a two-way filter
Problem formulation

(Wakileh, 2001 b). In the case of this research the delta connected isolation transformer will prevent zero sequence harmonics originating from the six-pulse thyristor rectifier propagating upstream of the isolation transformer and impacting on the GTA and the EM railgun support load. It should be noted that this is only possible if the transformer is connected in delta and not star. Additionally, an electromagnetic shield is usually employed between the primary and secondary windings to attenuate high frequency harmonics and to provide galvanic isolation. The transformer also serves to increase the six-pulse thyristor rectifier commutation reactance which also reduces harmonic waveform distortion, a concept which will be discussed further in section 3.4.1.

Importantly, the six-pulse thyristor rectifier selected to control the rate of charge of the ESD uses thyristor semiconductor devices which were discussed in section 2.2.2. As summarised in Table 2-1 thyristors are available as high power robust devices having a low voltage drop in the on-state. This means they have lower conducting losses when compared to other types of power electronic devices such as IGBTs (Bradley, 2009 b). Furthermore this type of rectifier is simple to control and has a low device count meaning that it is power dense (Lorenz, 2008), further supporting the justification for selecting this type of rectifier. It is acknowledged that an alternative power converter could have been employed however based on the literature review the six-pulse thyristor rectifier is considered the most suitable for this application. As such, a qualitative summary of alternative power converters with their associated advantages and disadvantages is provided in Table 3-2. The advantages and disadvantages of the six-pulse thyristor rectifier have also been included for the purposes of comparison.

<table>
<thead>
<tr>
<th>Power converter</th>
<th>Advantages</th>
<th>Disadvantages</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diode rectifier</td>
<td>High power rating</td>
<td>Uncontrollable</td>
</tr>
<tr>
<td></td>
<td>Simple and robust</td>
<td>Poor harmonic performance</td>
</tr>
<tr>
<td>Six-pulse thyristor rectifier</td>
<td>High power rating</td>
<td>Poor harmonic performance</td>
</tr>
<tr>
<td></td>
<td>Low loss</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Simple control</td>
<td></td>
</tr>
<tr>
<td>PWM rectifier (IGBT active front end harmonic filter)</td>
<td>Good harmonic performance</td>
<td>High loss</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Multiple devices required for high power</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Complex control</td>
</tr>
<tr>
<td>PWM rectifier (IGCT based active front end harmonic filter)</td>
<td>Good harmonic performance</td>
<td>High loss</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Complex control</td>
</tr>
</tbody>
</table>

While detailed description of six-pulse thyristor rectifiers is offered by Wildi (2006 a) and Bradley (2009 a), an explanation of the key characteristics which will impact on QPS during the charging of the ESD are offered in the following section. As the power system represented at Figure 3-9 is the power system on which this research will be based, a summary of the justification for the selection of the key warship power system and EM railgun system aspects and components is offered in Table 3-3 and Table 3-4 respectively.
### Table 3-3 Summary of the justification for the selection of the warship power system components

**Warship power system**

<table>
<thead>
<tr>
<th>System aspect</th>
<th>Justification for selection</th>
</tr>
</thead>
<tbody>
<tr>
<td>IFEP architecture</td>
<td>IFEP warships utilise common power generation for both propulsion and service loads and have the ability to generate high levels of power. IFEP solution allows propulsion power to be readily traded for high energy weapon power.</td>
</tr>
<tr>
<td>AC radial distribution system</td>
<td>AC radial distribution systems are typically employed on warships. Examples include the RN’s Type 45 Destroyer and Queen Elizabeth Aircraft Carrier.</td>
</tr>
<tr>
<td>36 MW GTA</td>
<td>Typical prime mover installed on IFEP warships. Examples include the Queen Elizabeth Aircraft Carrier and the DDG1000 Zumwalt Class Destroyer. Previous research in this field has been based on a 36 MW GTA.</td>
</tr>
<tr>
<td>5 MW GTA</td>
<td>Commensurate with the auxiliary power generation on candidate IFEP warships such as the DDG1000 Zumwalt Class Destroyer. Also commensurate with previous research in this field.</td>
</tr>
<tr>
<td>30 MW propulsion motors</td>
<td>Based upon the power and propulsive requirements of a candidate warship such as the DDG1000 Zumwalt Class Destroyer.</td>
</tr>
</tbody>
</table>

### Table 3-4 Summary of the justification for the selection of the EM railgun system components

**EM railgun system**

<table>
<thead>
<tr>
<th>System component</th>
<th>Justification for selection</th>
</tr>
</thead>
<tbody>
<tr>
<td>EM railgun</td>
<td>Recent announcement from the US Navy that an EM railgun may feature on an IFEP warship in the near future. Knowledge identified as lacking into the impact of EM railguns on warship electric power system QPS.</td>
</tr>
<tr>
<td>Isolation transformer</td>
<td>Simple and robust method of achieving harmonic attenuation. Increases commutation reactance (discussed further in section 3.4.1). Provides galvanic isolation between the GTA and EM railgun ESD charging system.</td>
</tr>
<tr>
<td>Six-pulse thyristor rectifier</td>
<td>Various instances reported whereby an uncontrolled rectifier has proved incapable of maintaining an acceptable standard of QPS when employed to charge pulsed load ESD. Simulation based research validated against a reduced power test bed has demonstrated that a six-pulse thyristor rectifier provides a simple and effective method of controlling the rate of charge of the ESD. High efficiency when compared with controlled rectifiers composed of other power electronic devices. Power dense solution.</td>
</tr>
<tr>
<td>160 MJ capacitive ESD</td>
<td>Currently the most technically mature medium and the lowest risk option for driving EM railguns. Validated capacitor characteristic information is readily available with which to conduct further research. 160 MJ capacitive ESD proved capable of powering EM railguns.</td>
</tr>
<tr>
<td>2 MW EM railgun support load</td>
<td>Commensurate with previous research in this field and representative of an EM railgun support load.</td>
</tr>
</tbody>
</table>
3.4.1 Thyristor rectifier charging control bridge characteristics

From the alternator perspective the load characteristic of the six-pulse thyristor bridge is important as regards the power factor, which will be discussed first in this section. The generator power factor is defined as the ratio of real power ($P$), to apparent power ($S$). When considering purely sinusoidal, fundamental voltage and current waveforms the difference between real power and apparent power is caused by inductance in the circuit, which gives rise to the reactive power component ($Q$). The relationship between real, apparent and reactive power is shown in Figure 3-10.

![Figure 3-10 Power triangle (McCauslin, 1984)]

The power factor can therefore be defined by Equation 11.

$$\text{Power factor} = \frac{P}{S} = \cos(\theta)$$  \[E11\]

Where $\theta$ is the power factor angle and represents a phase shift, or displacement, between the fundamental voltage and current waveforms, as shown in Figure 3-11. A circuit with a power factor of 1 is purely resistive and the apparent power drawn is equal to the real power. In such a circuit there is no phase shift between the voltage and current waveforms. A purely inductive circuit has a power factor of zero as it does not consume any real power. For the case shown in Figure 3-11 the power factor is said to be lagging, as the current lags behind the voltage. Essentially, power factor is considered a measure of the ability of an electrical distribution system to transfer useful power, with power factors approaching zero considered poorer than those approaching unity, due to the additional power loss occurring (McCauslin, 1984).

![Figure 3-11 Fundamental current and voltage phase relationship]
When considering purely sinusoidal waveforms the input real (P) and apparent (S) power can be defined by Equations 12 and 13.

\[
P = \frac{1}{T} \int_0^T v(t) i(t) dt \quad \text{yields} \quad P = V_{RMS} \cdot I_{RMS(1)} \cdot \cos(\theta_1) \quad \text{[E12]}
\]

\[
S = V_{RMS} \cdot I_{RMS(1)} \quad \text{[E13]}
\]

Where \( \theta_1 \) is the phase shift between the fundamental current and voltage.

However, when power electronic converters are employed, such as the six-pulse thyristor rectifier used to control the charging of the ESD in this research, the power factor takes on two new components. Firstly, as described by Bradley (2009 a) and Wildi (2006 a) a thyristor rectifier controls the mean value of the DC output voltage by delaying the firing of the thyristors by a set angle dependant on the required DC voltage, defined by Equation 14.

\[
V_{DC \,(\text{mean})} = 1.35 \cdot V_{RMS} \cdot \cos(\alpha) \quad \text{[E14]}
\]

Where \( V_{DC} \) is the mean DC output voltage of the rectifier; \( V_{RMS} \) is the Root Mean Square (RMS) value of the input AC voltage waveforms and \( \alpha \) is the thyristor firing delay angle. With reference to the circuit diagram of the six-pulse thyristor rectifier shown in Figure 3-12 \( V_{RMS} \) is the RMS value of the incoming AC waveforms labelled \( V_A \), \( V_B \) and \( V_C \).

![Six-pulse thyristor bridge](image)

\[ \text{Figure 3-12 Six-pulse thyristor rectifier circuit diagram} \]

In addition to controlling the mean value of the DC output voltage the firing delay angle increases the phase displacement between the load current and the input voltage waveforms thus is known as the phase control component (McCauslin, 1984). Hence, the total phase displacement is now composed of the reactive component and the phase control component, both of which pertain to the fundamental voltage and current. The second additional component arises due to the nature of the current drawn from the six-pulse thyristor rectifier, which ignoring overlap, is a quasi-square wave defined by Equation 15.

\[
I = \frac{2\sqrt{2}}{\pi} I_{DC} \cos(\omega t) - \frac{1}{5} \cos(5\omega t) + \frac{1}{7} \cos(7\omega t) - \frac{1}{11} \cos(11\omega t) + \frac{1}{13} \cos(13\omega t) - \ldots \quad \text{[E15]}
\]
This waveform contains harmonic components predominantly at 5, 7, 11 and 13 times the fundamental frequency which distort the AC supply waveforms. To further illustrate this concept, an example demonstrating the relationship between the three phase AC supply voltage waveforms, the six-pulse thyristor rectifier output voltage and the AC supply current waveforms is shown in Figure 3-13.

Assuming no filtering, Figure 3-13 shows the three phase AC supply waveforms and the DC output voltage of the six-pulse thyristor rectifier in the upper plot. The DC output voltage is shown as the black line in the upper plot and contains a visible ripple. The mean DC output is the mean value of this ripple. In this case the firing delay angle is 15 degrees. As shown in the second, third and fourth plots the AC input current waveforms are quasi-square waves comprising harmonic waveform distortion and are phase shifted 15 degrees with respect to the AC input voltage waveforms. The waveforms corresponding to a firing delay angle of 0 degrees and 45 degrees are shown in Figure A-1 and Figure A-2 respectively in Appendix A. From Figure A-1 it can be seen that the input AC current waveforms more closely resemble sin waves when the firing angle is 0 degrees and that the average DC output voltage is higher than the example shown in Figure 3-13. Figure A-2 shows that when the firing angle is increased to 45 degrees the input AC current waveforms are visibly more distorted and the average DC output voltage is lower than the example shown in Figure 3-13.

Figure 3-13 Relationship between the three phase AC supply voltage waveforms, DC output voltage and the AC supply current waveforms for a six-pulse thyristor rectifier – Firing angle 15 degrees
As such, the second component of the power factor deals with the power circulating at the harmonic frequencies present when the supply waveforms are distorted. These additional components exist in the XYZ plane (McCauslin, 1984) as shown in Figure 3-14, where $\alpha$ is the thyristor firing delay angle, $\theta$ is the phase displacement angle due to the reactive power component and $\Phi$ is the total power factor angle.

![Power triangle with harmonic component](McCauslin, 1984)

Figure 3-14 Power triangle with harmonic component (McCauslin, 1984)

Thus, for case where the waveforms are non-sinusoidal due to the non-linear effects of power electronic converters the phase relationship between the current and voltage is shown in Figure 3-15. For the case shown in Figure 3-15 the current waveform is defined by Equation 15 with the $11^{th}$ and $13^{th}$ harmonic negated.

![Distorted current and voltage phase relationship](Figure 3-15

Figure 3-15 Distorted current and voltage phase relationship

Figure 3-14 demonstrates that the apparent power is a three dimensional sum of the real power, reactive power and harmonic power present in the circuit. As shown, the harmonic power exists in the $Z$ axis, the mathematical derivation of which is presented by Rissik (1939).

Hence by taking the ratio of $P$ to $S$ for the case considered the total power factor is defined as:

$$\text{Power factor} = \frac{P}{S} = \frac{V_{\text{RMS}}I_{\text{RMS}}\cos(\theta)}{V_{\text{RMS}}I_{\text{RMS}}} \Rightarrow \frac{I_{\text{RMS}(1)}}{I_{\text{RMS}}} \cos(\alpha + \theta)$$  \[E16\]
Where \( \alpha \) is the thyristor bridge firing delay angle and \( \theta \) is the phase delay angle due to the reactive power component of the circuit. The total power factor angle is defined as \( \phi (\alpha + \theta) \), as shown in Figure 3-14.

The total power factor of the circuit considered in this research can therefore be defined in terms of the product of the ratio of fundamental current to total current drawn and the cosine of the firing delay angle, plus any additional phase displacement due to inductance in the circuit. The total power factor also takes into account power drawn at harmonic frequencies. This is different to the phase displacement factor, which is the power factor due to the phase shift between the fundamental voltage and current shown in Figure 3-10. As the load drawn by the thyristor rectifier charging bridge is much larger than the 2 MW EM railgun support load the total input displacement factor, shown in Figure 3-14, will be dominated by the firing delay angle (\( \alpha \)). This means that at large firing delay angles, closer to 90 degrees, the total power factor is low and the rectifier predominantly draws reactive current from the alternator. Furthermore, at firing delay angles closer to 90 degrees the harmonic waveform distortion is high thus the harmonic power component increases and the power factor decreases. At small firing delay angles closer to 0 degrees the power factor will increase due to the reduced reactive and harmonic power components, thus the rectifier predominantly draws real current (Kundur, 1994 d). These specific characteristics will impact directly upon the required AVR and governor responses when charging the ESD. This is because as discussed in sections 3.3.1 and 3.3.2, they control the reactive and real generator power delivery respectively.

Analysis of the six-pulse thyristor rectifier characteristics are further complicated when overlap is considered, which thus far has been ignored. Consider Figure 3-16 which shows the equivalent circuit of a six-pulse thyristor rectifier during a period of commutation, whereby thyristors 1, 3 and 2 are conducting at the same time. Non-conducting paths are shown in grey. During transition from phase A to B thyristor 3 is turning on and thyristor 1 is turning off. Due to the source inductance \( L_s \) which may comprise the inductance of the alternator, transformer and power cables, the phase currents cannot instantaneously change. The time taken for this change to occur is known as the overlap angle (\( \mu \)), during which current \( i_{sc} \) flows between phase B and A. The source impedance plays an important role in limiting the magnitude of \( i_{sc} \) which forms a line to line short circuit through impedance \( 2L_s \) (Kundur, 1994 d).

![Six-pulse thyristor rectifier equivalent circuit during commutation](Kundur, 1994 d)

The current loop formed between phase A and B shown in Figure 3-16 during commutation gives rise to the commutating voltage described by Equation 17.
Problem formulation

\[ e_b - e_a = L_s \frac{di_3}{dt} - L_s \frac{di_1}{dt} \]  
\[(E17)\]

The commutating voltage results in a reduction of the incoming AC voltage waveform during the period of commutation defined by Equation 18.

\[ V_A = V_B = e_b - L_s \frac{di_3}{dt} \]  
\[(E18)\]

This manifests as a visible notch in the incoming AC waveform voltage, as shown in the upper plot of Figure 3-17 which shows the relationship between the three phase AC supply voltage waveforms, the six-pulse thyristor rectifier DC output voltage and the AC supply current waveforms when commutation reactance is taken into consideration. This notching distorts the supply side AC waveforms. The depth of the notch is described by Equation 19.

\[ Depth\ of\ notch = \frac{V_A - V_B}{2} \]  
\[(E19)\]

Figure 3-17 Relationship between the three phase AC supply voltage waveforms, DC output voltage and the AC supply current waveforms for a six-pulse thyristor rectifier – Firing angle 15 degrees – Including commutation reactance
It should also be noted when considering Figure 3-17, that when compared with Figure 3-13 the rate of rise of the input AC current waveforms, or the \( \frac{di}{dt} \) at the point of switching is reduced. This is due to the commutation reactance preventing the current from changing across the device instantaneously and serves to soften the edges of the current waveforms. Hence, the current waveforms shown in Figure 3-17 more closely resemble a sinusoid when compared with those shown in Figure 3-13. This reduction in harmonic distortion of the current waveforms is a distinct advantage of commutation reactance.

3.4.2 ESD capacitor load characteristics

As shown in Figure 3-9, the six-pulse thyristor charging bridge load, which is the EM railgun ESD, is defined as a capacitor with an energy storage capacity of 160 MJ. This is the overall energy required per shot, as detailed in Table 3-1. A capacitive ESD was selected based on the results of simulation based (Bernardes, et al., 2003) (Wolfe, et al., 2005) and experimental (McNab, et al., 1995) research, which has demonstrated that capacitor based ESDs are capable of powering EM railguns.

From a load perspective the charging characteristics of any capacitor are:

\[
q = C \cdot V_{DC} \left[ 1 - e^{-\frac{t}{RC}} \right]
\]

Where \( R \) is the series resistance of the capacitor plus any line resistance.

And since \( I = \frac{dq}{dt} \) integrating with respect to time yields:

\[
I = \frac{V_{DC}}{R} e^{-t/RC}
\]

Where \( V_{DC} \) is the DC supply voltage; \( C \) is the capacitance and \( R \) is the Series Resistance (SR) of the capacitor plus any line resistance.

Hence, the charge level and current depends upon the DC voltage applied to the capacitor. The rate of charge depends upon the time constant \( RC \) and as such the most rapid charge is achieved with an appropriate balance of storage capacitors connected in series to minimise \( C \), and connected in parallel to minimise the SR, \( R \). In practice this paradox is resolved by the maximum available voltage \( V_{DC} \) which in turn depends upon the generator voltage and the maximum allowed current. Also the SR tends to be small, as demonstrated by Bernardes, et al., (2003) and Wolfe, et al., (2005).

Now consider than the output voltage from the six-pulse thyristor rectifier is defined as:

\[
V_{DC} = \frac{V_{\text{max}}}{\pi/3} \int_{\frac{\pi}{6} + \alpha}^{\frac{\pi}{6} + \alpha} \cos(\omega t) \, d(\omega t) \overset{\text{yields}}{\rightarrow} 1.35 \cdot V_{\text{RMS}} \cdot \cos(\alpha)
\]

Where \( V_{DC} \) is the mean DC output voltage, \( V_{\text{max}} \) is the maximum voltage and \( V_{\text{RMS}} \) is the RMS voltage of the supply. The charge of the capacitor is dependent upon \( V_{DC} \) hence:
And the ESD charging current drawn from the generator can be defined as:

\[ I = \frac{1.35V_{RMS}\cos\alpha}{R}e^{-\frac{t}{RC}} \]  

As such, the rate of ESD charge and its associated current are dependent upon the firing delay angle \(\alpha\), of the six-pulse thyristor charging bridge. Therefore as the alternator must operate within its P and Q capability curves, i.e. within the limits of the real and reactive power output (Kundur, 1994 b) and within its voltage and frequency stability limits when maintaining QPS, as defined by the AVR and governor respectively, defined control relationships exist. These control relationships will be developed further in the following section.

### 3.4.3 Control relationships between warship electric power system and EM railgun

During EM railgun firing operations the control of the GTA when charging the EM railgun ESD is facilitated as follows: The real power demand is controlled by the GT fuel flow; the GT governor enables speed control across the power range; the reactive power is controlled by the AVR and the current drawn is controlled by the rectifier firing angle delay \(\alpha\), which also dictates the charge rate of the ESD. These control relationships which are key in understanding the impact of EM railgun operations on warship electric power system QPS are summarised below.

- **GTA governor** → speed → frequency → real power
- **GTA AVR** → field current → voltage → reactive power
- **Thyristor rectifier charging bridge** → firing angle → DC current → charge rate of ESD

These control relationships are shown in the context of the warship electric power system considered in this research in Figure 3-18. The control relationships are colour coded for clarity. The QPS measurements of voltage, frequency and waveform quality are also shown.

![Figure 3-18 Control relationships between warship electric power system and EM railgun](image_url)
It has been analytically demonstrated that the rate of charge of the ESD is dependent upon the rectifier firing delay angle \( (\alpha) \). When considering Figure 3-18 it becomes apparent that the interaction of the control relationships will determine the warship electric power system QPS during EM railgun operations and that the level of QPS maintained will depend on the rate of charge of the ESD thus the firing delay angle \( (\alpha) \). This is because the firing delay angle controls the DC charging current which comprises real and reactive components when drawn from the GTA. The demand for the real and reactive components of the ESD charging current impact on the voltage and frequency elements of QPS, the transient nature of which will depend on the response of the GTA governor and AVR respectively. From the analytical analysis presented in this section it can be concluded that during the early stages of the ESD charging cycle \( V_{DC} \) will be low, the rectifier firing angle delay \( \alpha \) will be high and as such the current drawn will be predominantly reactive. Owing to this the power factor of the alternator will be low. Also, the levels of harmonic waveform distortion will be high thus the waveform quality will be poor. In the latter stages of the ESD charging cycle \( V_{DC} \) will approach the RMS voltage of the supply and the rectifier firing angle delay \( \alpha \) will decrease, as such the current drawn will be predominantly real and the alternator power factor will increase. The quality of the waveform will also improve.

3.5 Electric warship QPS considerations in the context of EM railgun operation

Whilst thus far QPS has been discussed in terms of constancy of voltage and frequency and harmonic waveform distortion, there are specific standards that govern the QPS of electric power supplies in warships. Ensuring acceptable levels of power quality on warships is crucial in ensuring the power system integrity (Thongam, et al., 2013). Furthermore the compliance with QPS standards ensures compliance with and the proper operation of electrical machines and equipment connected to the supply. As was concluded from section 2.2.3 in Chapter 2, NATO STANAG 1008 is the only appropriate standard against which to quantify the impact of EM railguns on QPS in this research. This is because the aim of STANAG 1008 is to provide operational compatibility between warships of NATO navies by specifying a mutually agreed standard. As such the alternative standards considered in section 2.2.3 are aligned with NATO STANAG 1008. Hence, assessing the impact of EM railguns against this standard makes this research relevant for all NATO navies which may consider implementing EM railguns in the future. As summarised in Table 3-5, NATO STANAG 1008 governs acceptable voltage and frequency deviations under steady state, transient and maximum transient conditions with an associated recovery time also being stipulated under transient and maximum transient conditions. NATO STANAG 1008 also governs waveform quality, with a limit being placed on the maximum allowable THD under steady state conditions.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Steady state tolerance</th>
<th>Transient tolerance</th>
<th>Maximum transient tolerance</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage</td>
<td>±5%</td>
<td>±16% (2 s)</td>
<td>±20/-20% (2 s)</td>
</tr>
<tr>
<td>Frequency</td>
<td>±3%</td>
<td>±4% (2 s)</td>
<td>±5.5% (2 s)</td>
</tr>
<tr>
<td>Voltage THD</td>
<td>5%</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

Table 3-5 NATO STANAG 1008 QPS standard summary (NATO, 2004)
3.6 Summary

This section has provided background reading and analytical analysis into the problem posed by integrating EM railguns with warship electric power systems which are governed by QPS standards. Firstly, the concept of EM railgun operation was described as a two stage process being the charging stage during which energy is transferred from the ship’s power system to the ESD and the firing stage during which energy is transferred from the ESD to the projectile via the PFN. The ESD charging and recharging stages were identified as being of interest in this research, as these stages will instigate the transient disturbances that will impact on QPS. Adopting a stability analysis approach the GTA control systems responsible for maintaining the voltage and frequency elements of QPS, being the generator AVR and governor respectively, were described. The characteristics suitting the selected type of AVR and governor to the problem considered in this research were also considered. Expanding on the operation of the control systems their ability to manage the demands of charging the EM railgun ESD were mathematically explored, with it being concluded that in addition to their QPS maintenance responsibilities, the AVR and governor must meet the reactive and real power demand of the ESD charging respectively, at the rate commensurate with EM railgun firing operations.

It was suggested that a six-pulse thyristor rectifier be employed to regulate the ESD charging cycle. This type of power electronic converter was selected for its high efficiency when compared with controlled rectifiers composed of other power electronic devices and its relative ease of control. Furthermore, the six-pulse thyristor rectifier has been proven an effective method of controlling the rate of charge of the ESD in previous research reviewed in Chapter 2. In relation to the characteristics of the six-pulse thyristor rectifier it was analytically demonstrated that the firing delay angle $\alpha$ will not only dictate the rate of charge of the ESD, but will also impact on the reactive power demand and thus the alternator power factor over the charging cycle. It was also demonstrated that the six-pulse thyristor rectifier will introduce harmonic waveform distortion into the warship’s AC electric power system during EM railgun operations which will distort the AC supply voltage and current waveforms. It was concluded that while the current NATO STANAG 1008 QPS standard does not make provision for the operation of EM railguns, the system should be designed to operate within defined standards. As no specific standard exists, it was concluded that NATO STANAG 1008 would make for an appropriate starting point against which to assess the impact of EM railgun operations on QPS. Methods to fully explore the transient and harmonic impact of EM railgun operations on warship electric power system QPS will be discussed in the following chapter.
Chapter 4 Modelling

4.1 Introduction

As described in Chapter 3, it is necessary to understand the transient and harmonic impact of the EM railgun and its associated ESD charging system on the QPS of a candidate warship electric power system. Chapter 3 described this problem mathematically from a transient stability perspective and described the harmonic impact of the six-pulse thyristor rectifier. As such, a suitable method of analysis is required to capture the power system transients and harmonic waveform distortion across the time period pertaining to the operation of the EM railgun so that the impact on QPS may be explored further. In contrast to empirical calculations previous research in this field conducted by Sudhoff, et al (2004), Lewis (2006), Kanellos, et al., (2007), Tsekouras, et al., (2010) and Kanellos, et al., (2011) have successfully implemented time-domain simulations as a tool to assess the impact that transient disturbances have on QPS resulting from EM railgun operations. As opposed to reduced scale laboratory based research which presents significant challenges, computer based modelling and simulation is now a widely accepted method for analysing the design of electric warships, as described by Deverill, et al., (2003) Bennet, et al., (2007) and Norton, et al., (2007) and will be employed as the research method here. A further advantage of this approach is that the significant expense associated with conducting full scale experimental research with candidate warship power systems at facilities such as the ESTD is avoided until key design issues have been agreed.

To be an effective research method, yielding credible simulation results that aid the understanding of performance, a thorough understanding of the model’s equations, parameters and limitations is required. Furthermore the equipment models employed should undergo validation or verification to ensure they are sufficiently representative of a warship electrical power system. Hence, building on the analysis presented in Chapter 3 this chapter will describe the modelling tool constructed to explore the impact of EM railguns and their associated charging systems on the QPS of a candidate warship electric power system. Beginning with a justification of the software package selected in which to build the modelling tool, this chapter will describe the system under investigation and the modelling assumptions made. Following this, a description of each of the constituent models comprising the overall tool will be offered, which in each case includes the model schematic or equivalent circuit, the justification of and selection of the model parameters and the
model limitations, where appropriate. This chapter ends with the validation and verification of the modelling tool.

4.2 Software selection

The software package selected in which to build the modelling tool was MATLAB®/Simulink®, together with the power systems blockset library SimPowerSystems, which facilitates the simulation of electrical power system components. While other options such as Power Systems Computer Aided Design (PSCAD) software were considered, MATLAB®/Simulink® was selected to allow the integration of existing and highly relevant models, provide continuity with previous research in this field and to align with the industry standard. This promotes the usefulness of the modelling components developed in this research to naval industry. Further justification for the software selection can be found at Appendix B.

4.3 Warship electric power system performance tool

Thus far, it has been concluded that modelling and simulation is an effective and appropriate method of conducting research into the transient and harmonic impact of EM railguns on warship electric power system QPS. Furthermore, MATLAB®/Simulink® has been selected as the software package in which to conduct this simulation based research. Despite an extensive library of warship electric power system components being available no complete modelling tools exist to assess the research problem formulated in Chapter 3. As such, a MATLAB®/Simulink® based tool capable of simulating the transient and harmonic impact of EM railguns on warship electric power system QPS, referred to herein as the performance tool, was developed. Before the performance tool was constructed it was necessary to define the modelling purpose and requirement.

4.3.1 Modelling purpose

The purpose of modelling the warship electric power system is to explore the transient and harmonic impact of EM railguns on QPS. The impact on QPS depends on the interaction between three key control devices and methods, as described in section 3.4.3. These control relationships are the GTA AVR response, GTA governor response and the thyristor charging bridge firing delay angle, the complete control system for which will be described in section 4.5.2. As described in section 3.4.3 the interaction of these control relationships will determine the transient and harmonic impact on QPS from a system perspective. Hence, the purpose of modelling the warship electric power system is to simulate the interaction between the GTA AVR, the GTA governor and the ESD charging control system such that the combined impact on QPS during the time period pertaining to EM railgun operations may be understood.
4.3.2 Modelling requirement

The warship electric power system under investigation in this research is as presented in Figure 3-9 and is described in section 3.4. Owing to the open bus configuration of the radial power system during EM railgun operations the modelling of the entire system is not required and doing so would unnecessarily increase the simulation run time. Also as stated in section 3.2 only the first part of the energy transfer process, whereby energy is transferred from the GTA to the ESD, is of interest in this research as this stage will impact on the transient performance, hence QPS of the supply system. Therefore, the modelling of the PFN and EM railgun itself is not necessary. Hence, the modelling requirement can be considered that section of the system highlighted by the light grey shaded area in Figure 4-1. The boundaries of the model are considered to extend to the circuit breakers labelled A, B, C and D in Figure 4-1 which are assumed to be open. The circuit breakers are not included in the model. The model also encompasses the 160 MJ ESD.

This circuit could be aligned to any warship employing a multiple island radial electric power distribution system, as shown in Figure 4-1, deriving power from a GTA commensurate with the rating employed in this research. The GTA model employed in this research is characteristic of the GTAs likely to be employed on future warships, the choice of which is realistically limited to the Rolls-Royce MT30 and the GE LM6000 which are both rated at 36 MW (Beno, et al., 2004). Hence, the circuit identified in Figure 4-1 is commensurate with the electric power system of future warships likely to employ EM railguns, thus supporting the strength of this research. The investigation approach adopted could also be repeated with other circuit components or parameters and the results compared with those of this research when seeking to examine a similar warship electric power system.
It therefore becomes apparent that the power system configuration under which EM railgun operations would be conducted, which is of interest in this research, can be simulated using a simulation based performance tool comprising the following models.

1. 36 MW GTA
2. Power cables and isolation transformer
3. Six-pulse thyristor rectifier (ESD charging circuit)
4. 160 MJ capacitive ESD
5. 2 MW EM railgun support load

The justification for selecting each of the previously listed component models is offered in Table 3-3.

4.3.3 Modelling assumptions and limitations

When using the performance tool to conduct this simulation based research, the following assumptions and limitations apply and are deemed acceptable when considering the modelling purpose.

Modelling assumptions
1. The EM railgun operation commences with zero stored energy in the ESD, which is considered to be inherently safe.
2. It is accepted that propulsion power will be sacrificed when operating the EM railgun for significant periods and that top speed will not be achievable as only one GTA will be providing propulsive power.
3. At the end of the EM railgun operation minimal excess energy should be generated by the GTA. This means that there is a lower amount of energy which needs to be dissipated following a shot. This increases the efficiency of the overall EM railgun system.
4. The EM railgun shot time is negligible when compared with the ESD charge time.
5. Each shot will discharge 160 MJ from the ESD but this may not necessarily determine the capacity of the ESD.
6. The ship’s power system and the EM railgun system remain healthy throughout the EM railgun operation. System faults such as a capacitor failure, a thyristor misfiring or a main bus fault during the charging of the EM railgun would impact on the performance of the power system. Hence, the conclusions drawn from this research are valid only for healthy system conditions.

Modelling limitations
1. The models employed in this research do not allow the examination of performance when subject to increased temperatures. As such, the conclusions drawn on the impact of EM railgun operations on
QPS are limited to the electrical perspective only and do not provide a guarantee of the power system performance from a mechanical or thermal standpoint.

2. The GTA model employed in this research has an inbuilt load shed logic feature. This feature is designed to manage the GTA fuel flow during loss of load at mid to high power to prevent excessive over-speed whilst preventing flame out. This is a necessary safety feature built into the model which cannot be disabled and as such was pertinent to include to ensure practical limitations were observed, as confirmed in the letter from Rolls-Royce at Appendix E. Hence, the conclusions drawn from this research on the capacity of the ESD required to maintain QPS when conducting continuous EM railgun firing operations are valid within the constraints of the GTA load shed logic.

3. The firing constraints recommended to maintain acceptable QPS during continuous firing operations are valid for regular shot patterns only, whereby the ESD is recharged at a constant, pre-defined rate between shots which is determined by the limitations of the GTA. This is due to the controller developed for use in this research, which as described in section 4.5.3 can execute pre-defined shot patterns only.

4.3.4 Performance tool system diagram

The system diagram of the performance modelling tool developed is shown in Figure 4-2. The top level model schematic of the performance tool as constructed in Simulink® can be found at Figure C-3 in Appendix C. The model consists of a 36 MW GTA with associated AVR and governor control systems; power transmission cables; isolation transformer; the EM railgun ESD charging circuit, which consists of the six-pulse thyristor rectifier and control system and the EM railgun ESD. A description of each of the constituent parts is given in sections 4.4 to 4.7.

![Figure 4-2 Performance tool system diagram](image-url)
4.4 Gas turbine alternator model

The GTA model is representative of a 36 MW GTA installed on a candidate warship likely to field EM railguns in the near future (LaGrone, 2015) (naval-technology.com, 2015). The GTA model consists of a GT and governor model, a synchronous alternator model and an AVR model.

4.4.1 Gas turbine and governor model

The GT and governor were supplied as a black box model by Rolls-Royce and consists of a GT engine model, a governor model and originally a load model, configured as shown in the model schematic in Figure 4-3. The actual Simulink model schematic can be found at Figure C-4 in Appendix C. The GT engine and governor model are based on the Rolls-Royce MT30 GT, further details of which are offered by Tooke & Kok (2008) and Rolls-Royce (2014). The governor consists of a PI controller as shown in Figure 3-8. The GT engine and governor model is configured as a triggered sub-system which enables the model specific sample time to be set independently of the solver step time. This allows the replication of the actual GT governor controller sample time, while allowing for an independent optimum time step to be employed for the rest of the simulation. The load model represents the mechanical characteristics of an alternator to which a real power demand can be applied.

The GT and governor model supplied by Rolls-Royce contains an inbuilt load shed logic feature. A load shed is considered a sudden loss of load occurring when the GT is operating at mid to high power which results in excessive GT over-speed. An essential control requirement during such a load shed is that the GT does not excessively over-speed and become unstable. As such, if a load shed occurs at mid to high power the control system takes emergency action to temporarily reduce the fuel flow while ensuring the engine does not flame out. This is representative of a practical GT operating constraint. This load shed logic feature contained within this GT model cannot be disabled. How the GT load shed logic impacts on this research will be discussed further in section 5.4.1 and section 5.4.2.

![Figure 4-3 GT and governor model schematic as supplied by Rolls-Royce](image-url)
The input to the load model is a load schedule which is a predefined, real power demand function of time. This is subtracted from the generator delivered power to yield the net power, which is divided by the alternator mass moment of inertia to obtain the rotational acceleration in rev/min/s. This is then integrated to find the rotational speed, which is fed back into the GT governor model. The model supplied by Rolls-Royce was validated against actual test bed results involving typical load steps and one case of a full loss of electrical load from 36 MW, which confirmed a high level of model to test bed agreement. A letter from Rolls-Royce confirming this level of validation can be found at Appendix E.

To be effectively employed in this research the as supplied GT and governor model previously described underwent modification to include a synchronous alternator in place of the closed loop mechanical load model shown in Figure 4-4. This modification, which was undertaken as part of this research, allowed the simulated generation of three phase voltage and reactive power delivery which could not be simulated with the original model thus expanding the original GT model to a GTA model. This upgrade was required to simulate the electrical interface between the six-pulse thyristor rectifier used to charge the EM railgun ESD and the warship electric power system. The power output from the GT model was used as the input mechanical power to the synchronous alternator, with the speed feedback loop remaining as per the original model. The actual GTA Simulink model schematic can be found at Figure C-5 in Appendix C. The synchronous alternator model is described further in the following section.

A description of the GT and governor model parameters shown in Figure 4-4 is given below and has been divided into model inputs and outputs, GT engine model parameters and GT governor model parameters for the purposes of clarity.

**Model inputs and outputs**

*Speed feedback* is the speed of the synchronous alternator (pu) which is fed back into the GT governor;

*DELP* is the GT developed mechanical power (MW) which is used to drive the synchronous alternator.

**GT engine model parameters**

*NL_RPM_dem* is the GT governor speed set point (rpm); *Droop_Enable_True* is used to set the control mode to droop or isochronous (BOOLEAN); *Anticipated_Loadshed* can be used to trigger a load shed at a
set time (s); \( PAMB_{kPa} \) is the ambient pressure of the GT engine air intake (kPa); \( TAMB \) is the ambient temperature of the GT engine air intake (K).

**GT governor model parameters**

Owing to the fact that the GT governor model was delivered as a black box the governor cannot be viewed directly. However, it is known that the governor is a PI controller as shown in Figure 3–8, where the parameter \( FC_{KNL1} \) is the GT governor proportional gain and \( FC_{KNL2} \) is the GT governor integral gain. This was confirmed with Rolls-Royce, the evidence for which can be found in the letter at Appendix E.

### 4.4.2 Synchronous alternator model

The synchronous alternator forms an integral part of the 36 MW GTA model as it is responsible for generating the electrical power from which the ESD is charged. As was identified through Chapter 2 the alternator sub-transient reactance has a significant influence over the impact of pulsed loads on QPS. As such, the quasi-steady state per phase equivalent circuit of a synchronous alternator was presented in section 3.3.1 and the impact of the transient and sub-transient reactance on the alternator voltage and current explained. However, as discussed earlier to capture the power system transients across the time period pertaining to the operation of the EM railgun time-domain simulations are required. As the synchronous alternator models presented in section 3.3.1 are quasi-steady state, further development of the models is required to allow the transient performance characteristics to be explored. As such, this section will provide the background theory behind the transient representation of a synchronous alternator required for this research.

A commonly used transient synchronous alternator model, and one that has been employed in previous research in this field (Kanellos, et al., 2007) (Tsekouras, et al., 2010), is the d and q axis representation which aims to simplify the analysis of three phase synchronous alternators. While a full explanation of d and q axis representation is offered by Kundur (1994 g), this section will provide sufficient reading required to understand the model employed in this research. Consider the cross sectional diagram of a synchronous alternator given in Figure 4–5, which shows a three phase, two pole machine. The magnetic field produced by the DC field winding rotates at speed \( \omega_r \) (rpm), driven in this case by the speed at which the GT drives the rotor.
The stator windings are physically separated by 120°. As such the resulting mmf waveforms induced in the stator windings are displaced by 120°. Thus, for constant rotation of the rotor under balanced steady state conditions the resulting voltages and currents induced in the stator are displaced by 120° in time. Hence, the three phase currents can be expressed by Equation 25.

\[
\begin{align*}
i_a &= I_m \sin(\omega_s t) \\
i_b &= I_m \sin(\omega_s t - \frac{2\pi}{3}) \\
i_c &= I_m \sin(\omega_s t + \frac{2\pi}{3})
\end{align*}
\]

Where \( I_m \) is the peak magnitude of the resulting mmf waveform, \( \omega_s \) is the angular frequency of the stator currents in rad/s and \( t \) is a given instance in time.

Under balanced steady state conditions the magnetic field produced by the field winding rotates at synchronous speed, which for a 60 Hz machine is 3600 rpm. When providing constant power the magnetic field produced in the rotor, and the mmf waveforms induced in the stator, must rotate at the same speed (Kundur, 1994 h). However, under transient conditions \( \omega_r \) may not be constant due to variation in the speed of the prime mover, considered in this case to be the GT. As such, the magnetic field produced by the DC field winding may vary with respect to time. Hence, the magnetic field rotating at the speed of the rotor (\( \omega_r \)) may rotate at a different speed to that of the mmf induced in the stator (\( \omega_s \)). Furthermore, under transient conditions eddy currents may flow in the rotor surfaces, slot walls and in the amortisseur windings of the machine (Kundur, 1994 h).

Amortisseur, or damper windings consist of brass or copper rods embedded into the pole faces which form short circuited windings, the intention of which is to dampen out oscillations by providing a path for circulating eddy currents that arise under transient conditions. Under steady state conditions rotor current
only exists in the direct axis and as such the amortisseur windings have no impact on the equivalent circuit (Kundur, 1994 h). However, as this research is concerned with transient stability analysis, the amortisseur windings have been taken into account. To account for the previously discussed transient characteristics, the d and q axis model transforms the three phase AC rotor and stator quantities into a single rotating reference frame which rotates with the rotor. This can be considered a method of referring the stator quantities to the rotor such that the stator circuits rotate at the same speed as the rotor circuits under transient conditions (Kundur, 1994 h). In aiding the understanding of this process consider Figure 4-5 in which it is apparent that the rotor has two axes of symmetry. These axes of symmetry are the d axis, centred magnetically in the centre of the north pole and the q axis, which is 90 degrees (electrical) ahead of the d axis. As shown in Figure 4-5 the position of the rotor relative to the stator can be considered in terms of the angle \( \theta \), measured between the d axis and the a phase axis.

Consider now Figure 4-6 which shows the d and q axis rotating reference frame, rotating at angular velocity \( \omega_r \), considered in this case to be electrical rad/s. The rotor circuit comprises the field and amortisseur windings while the stator circuit comprises the three phase stator windings.

![Rotor and stator circuits of synchronous alternator](Kundur, 1994 h)

Where the notation as according to Kundur (1994 h) is as follows:

- \( e_{a,b,c} \) – Stator phase winding voltages
- \( \psi_{a,b,c} \) – Stator phase winding flux linkage
- \( i_{fd} \) – Field winding current
- \( e_{fd} \) – Field winding voltage
- \( i_{kd} \) – d-axis amortisseur winding current (where \( k \) is the number of amortisseur windings)
- \( i_{kq} \) – q-axis amortisseur winding current (where \( k \) is the number of amortisseur windings)
- \( \theta \) – Angle by which the d-axis leads the magnetic axis of phase a winding

And where the relationship between the instantaneous value of flux linkage (\( \psi \)) is related to the terminal voltage (\( e_a \)) shown in the stator circuit in Figure 4-6 by Equation 26.
\[ e_a = \frac{d\omega}{dt} + r_a i_a \]  \[ \text{[E26]} \]

And to the winding inductance (\( L_a \)) by Equation 27.

\[ \psi = L_a i_a \]  \[ \text{[E27]} \]

Where \( r \) is the resistance of the winding as shown in Figure 4-7.

![Figure 4-7 Stator winding circuit diagram](image)

Under transient conditions the eddy currents induced in the rotor and in the amortisseur windings are considered present in two sets of windings, one with flux in line with the d axis and one with flux in line with the q axis, as shown in Figure 4-6. It is important to note that the rotor field winding exists in the d axis component only, thus the q axis component is composed of two sets of amortisseur windings. In practice a large number of circuits are required to model the impact of amortisseur windings, however for the purposes of simplicity one in each of the d and q axis is considered acceptable.

As the rotor and stator circuits are magnetically coupled (Kundur, 1994 h), the stator quantities can be expressed in terms of the rotating d and q axis reference frame. An example expressing the stator currents \( i_a, i_b \) and \( i_c \) in terms of \( i_d \) and \( i_q \) is given by Equation 28 and Equation 29.

\[ i_d = k_d \left[ i_a \cos \theta + i_b \cos \left( \theta - \frac{2\pi}{3} \right) + i_c \cos \left( \theta + \frac{2\pi}{3} \right) \right] \]  \[ \text{[E28]} \]

\[ i_q = -k_d \left[ i_a \sin \theta + i_b \sin \left( \theta - \frac{2\pi}{3} \right) + i_c \sin \left( \theta + \frac{2\pi}{3} \right) \right] \]  \[ \text{[E29]} \]

Substituting Equation 25 for \( i_a, i_b \) and \( i_c \) into Equation 28 yields:

\[ i_d = k_d \left[ I_m \sin(\omega_s t) \cos \theta + I_m \sin \left( \omega_s t - \frac{2\pi}{3} \right) \cos \left( \theta - \frac{2\pi}{3} \right) + I_m \sin \left( \omega_s t + \frac{2\pi}{3} \right) \cos \left( \theta + \frac{2\pi}{3} \right) \right] \]

\[ i_d = k_d \frac{3}{2} \left[ I_m \sin(\omega_s t - \theta) \right] \]

Similarly, substituting Equation 25 for \( i_a, i_b \) and \( i_c \) into Equation 29 yields:

\[ i_q = -k_d \frac{3}{2} \left[ I_m \cos(\omega_s t - \theta) \right] \]
The constants $k_d$ and $k_q$ are arbitrary, however in most cases are considered to be 2/3 such that the peak values of $i_d$ and $i_q$ are equal to the peak values of the stator currents $i_a$, $i_b$ and $i_c$ (Kundur, 1994).

When retrieving the three phase currents $i_a$, $i_b$ and $i_c$ from the transformed $i_d$ and $i_q$ components it becomes apparent that a third term must be introduced. This is because $i_d$ and $i_q$ must be transformed back into $i_a$, $i_b$ and $i_c$. As under balanced conditions currents $i_a + i_b + i_c = 0$, the third component introduced is $i_0 = 0$. Thus, abc components are transformed into d, q and 0 components. This is commonly referred to as the Dq0 transformation. The purpose of the Dq0 transformation is to simplify the analysis of synchronous alternators required to conduct transient, time-domain simulations. The Dq0 transform for the positive sequence is given by Equation 30 in matrix form to facilitate execution in MATLAB.

$$
\begin{bmatrix}
    i_d \\
    i_q \\
    i_0
\end{bmatrix} = \frac{2}{3} \begin{bmatrix}
    \cos \theta & \cos \left( \theta - \frac{2\pi}{3} \right) & \cos \left( \theta + \frac{2\pi}{3} \right) \\
    -\sin \theta & -\sin \left( \theta - \frac{2\pi}{3} \right) & -\sin \left( \theta + \frac{2\pi}{3} \right) \\
    \frac{1}{2} & \frac{1}{2} & \frac{1}{2}
\end{bmatrix} \begin{bmatrix}
    i_d \\
    i_q \\
    i_0
\end{bmatrix}
$$

[Equation 30]

And the inverse transform for the positive sequence is given by Equation 31.

$$
\begin{bmatrix}
    i_a \\
    i_b \\
    i_c
\end{bmatrix} = \begin{bmatrix}
    \cos \theta & -\sin \theta & 1 \\
    \cos \left( \theta - \frac{2\pi}{3} \right) & -\sin \left( \theta - \frac{2\pi}{3} \right) & 1 \\
    \cos \left( \theta + \frac{2\pi}{3} \right) & -\sin \left( \theta + \frac{2\pi}{3} \right) & 1
\end{bmatrix} \begin{bmatrix}
    i_d \\
    i_q \\
    i_0
\end{bmatrix}
$$

[Equation 31]

The same set of transforms also apply to stator flux linkages and voltages which are required when executing the complete synchronous alternator equivalent circuit model.

The d and q axis simplified equivalent circuit of the synchronous alternator can now be developed, which extends the simplified quasi-steady state per phase synchronous alternator equivalent circuit presented at Figure 3-5 in Chapter 3. The d and q axis simplified equivalent circuit is shown in Figure 4-8, with the d and q axis circuits given separately in the upper and lower diagrams in the figure, respectively. These simplified circuits allow the expression of the flux linkage ($\psi_d$ and $\psi_q$) in terms of $i_d$, $i_q$ and the rotor variables. The inductance $L_{dlq} - L_{qd}$ represents the flux linking the field and amortisseur winding, but not the stator winding. It should also be noted that the field winding components exist only in the d axis and that the q axis circuit comprises two amortisseur windings (Kundur, 1994).
Where the notation as according to Kundur (1994 h) is as follows:

- $\psi_{d(q)}$ – Stator flux linkage (d and q axis)
- $e_{fd}$ – Field winding voltage (d axis only)
- $i_{fd}$ – Field winding current (d axis only)
- $R_{fd}$ – Field winding resistance (d axis only)
- $L_{fd}$ – Field winding inductance (d axis only)
- $L_{f1(d and q)}$ – Amortisseur winding 1 inductance (d and q axis)
- $R_{f1(d and q)}$ – Amortisseur winding 1 resistance (d and q axis)
- $L_{2q}$ – Amortisseur winding 2 inductance (q axis only)
- $R_{2q}$ – Amortisseur winding 2 resistance (q axis only)
- $L_{a(d and q)}$ – Flux linkage between the stator and amortisseur windings (d and q axis)
- $L_{f1d} - L_{ad}$ – Flux linkage between the field and amortisseur winding
- $L_{d}$ – Stator inductance (d and q axis)
- $i_{1(d and q)}$ – Amortisseur winding 1 current (d and q axis)
- $i_{2q}$ – Amortisseur winding 2 current (q axis only)

For the purposes of clarity, Figure 4-9 shows the flux linkage paths represented by the inductances shown in Figure 4-8. The modelling of the flux linkage paths is a key part of developing a transient model from the quasi-steady state models presented in section 3.3.1. A full representation of the flux linkage paths allows the simulation of the flux which links the sub-transient, transient and steady state circuits described...
in section 3.3.1 and the simulation of the circulating eddy currents which flow in the amortisseur windings under transient conditions.

The synchronous alternator model selected for use in this research utilises the SimPowerSystems blockset Synchronous Machine model, a full description of which is given by The MathWorks, Inc., (2014 c). The model represents both the electrical and mechanical characteristics of the machine. The electrical characteristics of the machine are based on the previously discussed Dq0 transform and the d and q axis equivalent circuits presented in Figure 4-8. Hence, the selection of this model is considered appropriate to model the transient characteristics of the synchronous alternator in this research. The actual model employed includes a modification to the simplified equivalent circuit presented at Figure 4-8, in that the stator resistances \( R_s \) and speed voltage terms \( \omega_s \psi_d \) and \( \omega_s \psi_q \) are introduced. The equivalent circuit of the SimPowerSystems blockset Synchronous Machine model is shown in Figure 4-10, as presented by The MathWorks (2014 c). In the circuit presented at Figure 4-10 the electrical quantities are viewed from the stator.

A list of the equations with which SimPowerSystems blockset calculates the voltage and flux linkage quantities, hence executing the Synchronous Machine model, are given in Appendix D. A description of the synchronous alternator model parameters is given below and has been divided into model inputs and
outputs. The stator winding parameters, field winding parameters and amortisseur winding parameters have been described previously in this section when describing Figure 4-8.

**Model inputs and outputs**

While the model inputs and outputs are not specifically shown in Figure 4-10 they are shown in the synchronous alternator Simulink model schematic at Figure C-6 in Appendix C.

*Peo* is the GTA output active power (pu); *Qeo* is the GTA output reactive power (pu); Frequency is the GTA voltage frequency (Hz); *V_a*, *V_b*, *V_c* (V) are the per phase alternator voltages, composed of the per phase *V_d* and *V_q* axis components shown in Figure 4-10; *I_a*, *I_b*, *I_c* (A) are the per phase alternator currents, composed of the per phase *I_d* and *I_q* axis components shown in Figure 4-10.

**Synchronous alternator mechanical characteristics**

The mechanical characteristics of the synchronous alternator are represented by Equation 32 and Equation 33 (The MathWorks, Inc, 2014 d). Equation 32 is derived from the swing equation and calculates the rotor speed variation, taking into account the inertia constant, the accelerating torque resulting from the applied load and the damping factor due to the amortisseur windings. The full mathematical derivation is offered by Kundur (1994 h). Equation 33 then computes the mechanical speed of the rotor by summing the speed variation with the rated reference speed.

\[
\Delta \omega_r(t) = \frac{1}{2H} \int_0^t (T_m - T_e) \, dt - K_d \Delta \omega_r(t) \quad \text{[E32]}
\]

\[
\omega_r(t) = \Delta \omega_r(t) + \omega_0 \quad \text{[E33]}
\]

Where \( \Delta \omega \) is the speed variation with respect to speed of operation (rad/s); \( H \) is the alternator constant of inertia (s); \( T_m \) is the mechanical torque (Nm); \( T_e \) is the electromagnetic torque (Nm); \( Kd \) is the damping factor representing the effect of the amortisseur windings (pu); \( \omega_r(t) \) is the mechanical speed of the rotor (rad/s) and \( \omega_0 \) is the rated speed of operation (rad/s).

The model schematic for the mechanical part of the synchronous alternator is shown below in Figure 4-11 while the actual Simulink model schematic can be found at Figure C-7 in Appendix C.

![Synchronous alternator mechanical model schematic](The MathWorks, Inc, 2014 d)
4.4.3 AVR model

The AVR model utilises the SimPowerSystems blockset model of a standard IEEE ST2A excitation system, a full explanation of which is given by Kundur (1994 b) and The MathWorks, Inc., (2014 b). The model represents a compound source rectification system, a description and justification for the selection of which was given in section 3.3.1. The model schematic for the AVR model is given in Figure 4-12 and is based on the AVR shown in Figure 3-4 in section 3.3.1. The actual Simulink® model schematic can be found at Figure C-8 in Appendix C.

![Figure 4-12 IEEE Type ST2A excitation system model schematic (Kundur, 1994 b)](image)

A description of the AVR model parameters is given below and has been divided into model inputs and outputs, voltage controller parameters and exciter and rectifier parameters for the purposes of clarity.

**Model inputs and outputs**

- $V_{ref}$ is the reference voltage (pu);
- $V_T$ is the alternator terminal voltage (pu);
- $V_{UEL}$ is the initial value of the alternator terminal voltage (pu);
- $I_T$ is the alternator terminal current (pu);
- $e_{fd}$ is the output voltage applied to the field (pu).

**Voltage controller parameters**

- $K_A$ and $T_A$ are the voltage regulator gain and time constant and represent the main voltage regulator (s);
- $V_{RMAX}$ and $V_{RMIN}$ represent the maximum and minimum limits of the main voltage regulator (pu);
- $K_F$ and $T_F$ are the damping filter gain and time constant and represent the derivative feedback loop (s).

**Exciter and Rectifier parameters**

- $K_P$ and $K_I$ represent the potential and current circuit gain respectively; $K_E$ and $T_E$ are the exciter gain and time constant and represent the main exciter (s);
- $K_C$ represents the rectifier loading factor proportional to the commutating reactance; $E_{FDMAX}$ represents the limit on the exciter output due to magnetic saturation of the transformer (pu); $I_{FD}$ is the exciter field current (pu).
4.4.4 GTA model parameter selection

The following section details the parameters for each of the constituent models that comprise the GTA model. In each case the source from which the parameters have been derived is given.

**GT engine and governor parameters**

The GT engine parameters are representative of the operating requirements and conditions. The rpm demand is commensurate with that of a 2 pole 60 Hz generator which is 3600 rpm. The anticipated load shed is set to false as no load shed is anticipated. The droop enable signal is set to false, thus the generator is in isochronous control mode as it is operating as a single generator. The ambient temperature and pressure are based on ambient conditions as defined by ISO 2533, remembering that these conditions are those at the GTA air intake and not the engine room. The GT governor parameters are fixed within the GT engine and are as supplied and tuned by Rolls-Royce, as confirmed in the letter at Appendix E.

**Table 4-1 GT and governor model parameters**

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPM demand (NL_RPM_dem)</td>
<td>3600 rpm</td>
<td>Ambient temperature (TAMB)</td>
<td>288.15 K</td>
</tr>
<tr>
<td>Control method (Droop_Enable,True)</td>
<td>0 (BOOLEAN)</td>
<td>Governor P control gain (P)</td>
<td>0.56</td>
</tr>
<tr>
<td>Anticipated loadshed (Anticipated_Loadshed)</td>
<td>0 (BOOLEAN)</td>
<td>Governor I control gain (I)</td>
<td>6.52</td>
</tr>
<tr>
<td>Ambient pressure (PAMB_kPa)</td>
<td>101.33 kPa</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Synchronous alternator parameters**

The parameters for the synchronous alternator model, given in Table 4-2, have been taken from a datasheet supplied by Brush Electrical Machines which details the parameters for a synchronous alternator commensurate with the requirement of the GTA model employed in this research. The datasheet can be found at Appendix F. In the supporting the selection of the parameters detailed in Table 4-2 a comparison with the synchronous alternator parameters employed by Tsekouras, et al., (2010) when conducting research in this field will be made. A summary of the parameters employed by Tsekouras, et al., (2010), which it is argued are representative of a large marine alternator typically employed in IFEP warships, is given in Table 2-6 in Chapter 2. A comparison of the key characteristics identified by Tsekouras, et al., (2010) with those employed in this research is offered in Table 4-3.
Table 4-2 Synchronous alternator model parameters

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal power (Pn)</td>
<td>36 MW</td>
<td>Quadrature axis sub transient reactance (X&quot;q)</td>
<td>0.15 pu</td>
</tr>
<tr>
<td>Line voltage (Vn)</td>
<td>11 kV</td>
<td>Unsaturated zero sequence reactance (X0)</td>
<td>0.079 pu</td>
</tr>
<tr>
<td>Frequency (fn)</td>
<td>60 Hz</td>
<td>Stator resistance (Rs)</td>
<td>0.71 mΩ</td>
</tr>
<tr>
<td>Power factor (PF)</td>
<td>0.80</td>
<td>Transient open circuit time constant (T’d0)</td>
<td>9.50 s</td>
</tr>
<tr>
<td>Direct axis synchronous reactance (Xd)</td>
<td>1.95 pu</td>
<td>Transient short circuit time constant (T’d)</td>
<td>0.65 s</td>
</tr>
<tr>
<td>Direct axis transient reactance (X’d)</td>
<td>0.17 pu</td>
<td>Sub transient open circuit time constant (T”d0)</td>
<td>0.05 s</td>
</tr>
<tr>
<td>Direct axis sub transient reactance (X”d)</td>
<td>0.12 pu</td>
<td>Sub transient short circuit time constant (T”d)</td>
<td>0.04 s</td>
</tr>
<tr>
<td>Quadrature axis synchronous reactance (Xq)</td>
<td>1.78 pu</td>
<td>Moment of inertia (J)</td>
<td>970 kgm²</td>
</tr>
<tr>
<td>Quadrature axis transient reactance (X’q)</td>
<td>0.20 pu</td>
<td>Inertia constant (H)</td>
<td>1.53 s</td>
</tr>
</tbody>
</table>

Table 4-3 Comparison of key synchronous alternator model parameters with those employed by Tsekouras, et al., (2010)

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value employed by Tsekouras, et al., (2010)</th>
<th>Value employed in this research</th>
</tr>
</thead>
<tbody>
<tr>
<td>Direct axis synchronous reactance (Xd)</td>
<td>1.35 pu</td>
<td>1.95 pu</td>
</tr>
<tr>
<td>Direct axis transient reactance (X’d)</td>
<td>0.30 pu</td>
<td>0.17 pu</td>
</tr>
<tr>
<td>Direct axis sub transient reactance (X”d)</td>
<td>0.15 pu</td>
<td>0.12 pu</td>
</tr>
<tr>
<td>Sub transient open circuit time constant (T’d0)</td>
<td>0.05 s</td>
<td>0.05 s</td>
</tr>
<tr>
<td>Inertia constant (H)</td>
<td>1.50 s</td>
<td>1.53 s</td>
</tr>
</tbody>
</table>

As discussed in Chapter 2 Tsekouras, et al., (2010) identified the sub-transient characteristics of the machine as having a significant influence over the impact of pulsed loads on QPS. As seen in Table 4-3 the sub-transient characteristics of the alternator model employed in this research correlate well with the parameters selected by Tsekouras, et al., (2010) when conducting research in this field, thus increasing confidence in the suitability of the selected parameters. Furthermore, the parameters are representative of a large marine alternator thus are considered reasonable for use in this research.

While the power and voltage rating matches that of the GTA requirement it should be noted that the datasheet is for a 50 Hz alternator. To account for the 60 Hz requirement in this research the inertia constant was adjusted using Equation 34.

\[
H = \frac{1}{2} \frac{J \omega_0^2}{V_{A_{base}}} \tag{E34}
\]

Where H is the inertia constant (s); J is the moment of inertia (kgm²); \( \omega_0 \) is the rated angular velocity (rad/s); \( V_{A_{base}} \) is the base apparent power of the machine (VA).
Hence,

\[
H = \frac{1}{2} \frac{(970)(120\pi)^2}{45 \times 10^6}
\]

\(H = 1.53\) s, which is the inertia constant when adjusted for 60 Hz operation. This is the moment of inertia of the synchronous alternator only, the inertia of the GT is assumed negligible.

**AVR parameters**

The AVR parameters which are given in Table 4-4 were derived from those used in previous research conducted by Ferreira (2006) at UCL to model the power and propulsion system of the RN Type 45 Destroyer. As part of the research conducted by Ferreira (2006) a Rolls-Royce WR-21 GTA model was constructed, which comprised a standard IEEE ST2A excitation system, commensurate with that employed in this research. The AVR parameters used by Ferreira (2006) are based on the AVR employed at the ESTD, discussed in section 2.4 in Chapter 2. As the ESTD represents an actual candidate warship electric power system, the same parameters are considered appropriate and suitable for use in this research. While the research by Ferreira (2006) validated the AVR model based on the parameters given in Table 4-4 against real world test data from the ESTD, the model underwent independent validation for use in this research, as described in section 4.8.

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage regulator gain (K_A)</td>
<td>400</td>
<td>Potential circuit gain (K_P)</td>
<td>5 pu</td>
</tr>
<tr>
<td>Voltage regulator time constant (T_A)</td>
<td>16 ms</td>
<td>Current circuit gain (K_I)</td>
<td>8 pu</td>
</tr>
<tr>
<td>Voltage regulator output max (V_{RMAX})</td>
<td>20 pu</td>
<td>Exciter gain (K_E)</td>
<td>1</td>
</tr>
<tr>
<td>Voltage regulator output min (V_{RMIN})</td>
<td>0 pu</td>
<td>Exciter time constant (T_E)</td>
<td>0.8 s</td>
</tr>
<tr>
<td>Damping filter gain (K_F)</td>
<td>0.03</td>
<td>Rectifier loading factor (K_C)</td>
<td>1.82 pu</td>
</tr>
<tr>
<td>Damping filter time constant (T_F)</td>
<td>1 s</td>
<td>Exciter output limit (E_{FDMAX})</td>
<td>1 pu</td>
</tr>
</tbody>
</table>

As discussed in section 2.4 Tsekouras, et al., (2010) concluded that the AVR voltage regulator gain has a significant influence over the impact of pulsed loads on QPS. The AVR gain employed by Tsekouras, et al., (2010) was 200, while the AVR gain employed in this research which was obtained from the ESTD is 400. While it is acknowledged that the AVR gains are not equal, both are considered to be comparably high gain controllers.

### 4.4.5 GTA model limitations

The GTA model has the following limitations:

1. The GT model does not include a thermodynamic representation of the compressors and turbines.
2. The GT model is not intended to simulate start-up, shut down or idle and as such must be run with a minimum load of 0.3 MW at all times.
3. Load sheds greater than 18 MW applied over a period less than 20 ms may incorrectly trigger load shed logic. It is therefore necessary to apply load sheds greater than 18 MW over a minimum 20 ms time period. This is a limitation of this model.

4.5 EM railgun ESD charging circuit model

The EM railgun ESD charging circuit model consists of two parts. The six-pulse thyristor rectifier, with associated firing pulse generator and the charging circuit controller, both of which will be described in this section. The justification for the selection of the six-pulse thyristor rectifier was discussed in section 3.4. The six-pulse thyristor rectifier model will be described first.

4.5.1 Six-pulse thyristor rectifier and pulse generator model

The six-pulse thyristor rectifier model, shown in the right hand side of Figure 4-13, utilises the SimPowerSystems blockset model of a Universal Bridge, a full description of which is offered by The MathWorks, Inc., (2014 e). As shown in Figure 4-13 the universal bridge implements a three phase rectifier composed of 6 thyristors in a three leg configuration. The firing of the thyristors required to rectify the input AC voltage to the required DC voltage is enabled via a pulse generator shown in the left hand side of Figure 4-13. A full description of the pulse generator model is given by The MathWorks, Inc., (2014 a). The power electronic theory pertaining to the generation of the thyristor firing signals is well described by both Bradley (2009 a) and Wildi (2006 a). A description of the six-pulse thyristor bridge characteristics and typical waveforms were offered in section 3.4.1 in Chapter 3 thus are not repeated here. Importantly, this model simulates the harmonic waveform distortion resulting from the six-pulse thyristor rectifier, as was demonstrated by Figure 3-13 and Figure 3-17 in Chapter 3, which were plotted using the same model. Proper representation of the harmonic waveform distortion resulting from the six-pulse thyristor rectifier is key in assessing the harmonic impact of EM railgun operations on warship electric power system QPS.

![Figure 4-13 Thyristor bridge and pulse generator model schematic](image)

The pulse generator generates the thyristor firing pulses based on the firing angle delay $\alpha$ and on the synchronisation signal $\omega$ which is obtained from a Phase Locked Loop (PLL). This ensures that the
Modelling

thyristors are commutated in synchronism with the incoming AC voltage waveforms $V_{ABC}$. If the incoming AC waveforms are interrupted, for example due to a fault, the thyristors become susceptible to misfiring and commutation faults can occur in some cases (Jovcic, 2007). Hence, the quality of the supply side AC waveforms is critical in ensuring proper and synchronised thyristor commutation.

The firing delay angle $\alpha$ controls the mean magnitude of the thyristor bridge output DC voltage in accordance with Equation 35, as discussed in section 3.4.1. The impact of overlap will tend to reduce $V_{DC\,\text{mean}}$, as described in section 3.4.1.

$$V_{DC\,\text{mean}} = V_{\text{RMS}} \cdot 1.35 \cdot (\cos \alpha)$$  \hspace{1cm} [E35]

Where $V_{DC\,\text{mean}}$ is the mean thyristor rectifier output voltage (V); $V_{\text{RMS}}$ is the RMS value of the input AC line voltage (V); $\alpha$ is the firing delay angle (°).

4.5.2 EM railgun ESD charging circuit controller

As no controller suitable for controlling the rate of charge of an EM railgun ESD was available a bespoke controller was designed and implemented using fundamental Simulink building blocks. The operation of the ESD charging circuit controller will be discussed in the following section. The block diagram of the controller is shown in Figure 4-14. The Simulink model schematic is given in Figure C-9 in Appendix C.

Controller requirements

The three key controller requirements are as follows:

1. To control the ESD charging rate to facilitate rates of fire commensurate with EM railgun firing operations.

2. To prevent the over charging of the ESD.

3. To facilitate the GTA unload following the EM railgun firing operation.

![Figure 4-14 EM railgun ESD charging circuit controller model schematic](image-url)
The aim of the controller is to control the rate of charge of the ESD by controlling the thyristor firing delay angle $\alpha$, the premise for which was discussed in section 3.4.1 and 3.4.2. The ESD charge level is set via the capacitor voltage set point, the rate of rise of which is limited to limit the rate of charge. The aim of limiting the rate of ESD charge is to limit the power system transients during EM railgun operations. As concluded from Chapter 2 controlling the rate of charge of the ESD has been previously employed as a method to limit the power system transients resulting from EM railgun operations, albeit with limited success (Kanellos, et al., 2006) (Lewis, 2006). The corresponding firing angle is then calculated using Equation 35 and converted to degrees to match the requirement of the six-pulse thyristor rectifier pulse generator input. The same procedure is performed on the ESD feedback voltage signal to attain the feedback firing angle, which is then subtracted from the set point firing angle to generate an error signal. This error signal represents the difference between the charge demand and the actual charge hence synthesises the ESD charging current demand $I_{\text{charge}}$ (Sudhoff, et al., 2004). This error signal is then processed through a PI controller before being added to the set point firing angle via a feedforward control loop. The resulting firing angle is then limited to between 0 and 90 degrees. At 90 degrees the mean output DC voltage is 0 and at 0 degrees the mean output voltage is 1.35 times the RMS input AC line voltage, which is the maximum mean DC output voltage of the bridge. Hence, the firing angle effectively controls the synthesised ESD charging current demand $I_{\text{charge}}$.

When the feedback voltage is less than the voltage set point the feedback firing angle is greater than the set point firing angle and the resulting error signal is negative. Because the error signal is negative when it is added to the set point firing angle via the feed forward control loop $\alpha$ decreases thus $I_{\text{charge}}$ increases and the rate of charge of the ESD increases. This increases the ESD voltage level. As the proportional gain of the PI controller is set at a comparatively large value, this change is affected very quickly. Once the ESD voltage equals the set point the error between the set point and feedback firing angle is eliminated and, owing to the feedforward loop, the firing angle equals that of the voltage set point. Again, as the proportional gain of the PI controller is very high the firing angle increases very quickly to prevent over charging the ESD.

The firing delay angle $\alpha$ does not directly reflect the voltage of the ESD capacitor per se, but rather becomes a parameter to control the ESD charging current, based on the difference between the ESD capacitor voltage set point and feedback ESD voltage. The key factor in the operation of the controller is the very high proportional gain which makes for a very fast controller response time. This not only enables rapid charging of the ESD but also prevents over charging. The prevention of over charge is critical, as once energy has been transferred to the ESD it cannot be taken out until a shot is taken, thus over charging cannot be corrected.

A description of the charging circuit controller model parameters is given below and has been divided into model inputs and outputs and controller parameters for the purposes of clarity.

**Model inputs and outputs**

$V_{\text{Cap,ref}}$ is the ESD capacitor set point voltage which represents the required charge level (kV); $V_{\text{Cap}}$ is the ESD capacitor actual voltage which represents the actual charge level (kV); $\alpha$ is the thyristor firing delay
angle which is the input to the thyristor bridge pulse generator (°); \( \text{Rate\_lim} \) represents the rate at which the ESD voltage set point is allowed to rise thus limiting the rate of charge. \( \text{Rate\_lim} \) varies based on the rate of charge required.

**Model parameters**

\( K_P \) is the proportional gain of the PI controller and is set to 200; \( K_I \) is the integral gain of the PI controller and is set to 20. The controller proportional gain was intentionally set high to facilitate a rapid charge and to prevent over charging due to the fast response time, which was one of the key requirements of this controller. This approach is supported by Sudhoff, et al., (2004) who, as described in section 2.4 in Chapter 2, employ a similar system to control the rate of charge of the ESD. Hence, 200 was selected as a starting point. The parameters of the PI controller were then tuned on a trial and error basis. The verification of the ESD charging circuit controller is discussed further in section 4.8.2.

### 4.5.3 EM railgun ESD charging circuit model limitations

The EM railgun ESD charging circuit controller can execute pre-defined shot patterns only. Shot patterns may be a single shot or continuous shots at pre-defined intervals.

### 4.6 EM railgun ESD model

As discussed in Chapter 3, the EM railgun ESD has been defined as a capacitor with an energy storage capacity of 160 MJ. A 160 MJ capacitive ESD was selected based on the results of simulation based (Bernardes, et al., 2003) (Wolfe, et al., 2005) and experimental (McNab, et al., 1995) research, which has demonstrated that a capacitor based ESD of this energy capacity is capable of powering an EM railgun. The ESD model, the equivalent circuit of which is shown in Figure 4-15, was constructed from fundamental components in the SimPowerSystems blockset. The model consists of a capacitor with associated SR connected in parallel with a braking resistor employed to dissipate the power generated by the GTA when unloading following the EM railgun shot. The capacitor and braking resistor are switched in and out of the circuit via electronically controlled circuit breakers. The Simulink model schematic can be found at Figure C-10 in Appendix C.

![Figure 4-15 EM railgun ESD model equivalent circuit](image)

A description of the ESD model parameters is given below and has been divided into model inputs and outputs and ESD parameters for the purposes of clarity.
Modelling

Model inputs and outputs

$V_{\text{Cap}}$ is the capacitor voltage which is fed back into the ESD charging circuit controller (V).

ESD parameters

$C_{\text{store}}$ is the capacitance of the ESD (F); $R_{\text{SR}}$ is the series resistance of the ESD capacitor (Ω); $R_{\text{Brake}}$ is the resistance of the braking resistor (Ω).

4.6.1 EM railgun ESD model parameter selection

The EM railgun ESD capacitor parameters are based on those presented by Wolfe, et al., (2005) which describes the modelling of a 200 MJ PFN. The model developed by Wolfe, et al., (2005) has been validated against existing EM railgun shot data from actual test firings at the Green Farm Test Facility (McNab, et al., 1995) (Wolfe, et al., 2001). As this research is concerned only with the first stage of the energy transfer process, whereby energy is transferred from the GTA to the ESD, only the ESD part of the entire ESD and PFN model presented by Wolfe, et al., (2005) was required.

The ESD presented by Wolfe, et al., (2005) is realised through a modular approach, with each module consisting of 24, 11 kV, 130 kJ capacitors, making for a total stored energy of 3 MJ per module. Each module has an equivalent capacitance of 48.96 mF and a $R_{\text{SR}}$ of 541 uΩ. To store the 160 MJ required for the EM railgun shot under investigation in this research 54 modules are connected in parallel to form the equivalent ESD. A summary of the bank characteristics and the equivalent ESD parameters are given in Table 4-5. The braking resistor is rated to withstand the rated full load current of the GTA and was calculated as follows:

$$\text{Rated MW} = \sqrt{3}(V_L)(I_L)(pf) \quad [\text{E36}]$$

Hence

$$I_L = \frac{36 \times 10^6}{(11000)(I_L)(0.8)} = 2361 \text{ A}$$

And

$$V_L = I_L R_{\text{Brake}} \quad [\text{E37}]$$

Therefore

$$R_{\text{Brake}} = 4.66 \text{ Ω}$$
### Modelling

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Module storage capacity</td>
<td>3 MJ</td>
<td>ESD storage capacity</td>
<td>160 MJ</td>
</tr>
<tr>
<td>Module capacitance</td>
<td>48.96 mF</td>
<td>ESD Capacitance (C_{\text{store}})</td>
<td>2.64 F</td>
</tr>
<tr>
<td>Module SR</td>
<td>541 (\mu\Omega)</td>
<td>Capacitor SR (R_{\text{SR}})</td>
<td>10.02 (\mu\Omega)</td>
</tr>
<tr>
<td>Modules required</td>
<td>54</td>
<td>ESD braking resistor (R_{\text{brake}})</td>
<td>4.66 (\Omega)</td>
</tr>
</tbody>
</table>

#### 4.6.2 EM railgun ESD model limitations

The capacitor model considered in this research is a simplification of the equivalent circuit of a capacitor and does not take into account the equivalent leakage resistance. As this research is concerned only with the transfer of energy from the GTA to the ESD and not the storage of energy in the ESD for any significant amount of time, the modelling of the leakage resistance was not considered necessary.

#### 4.7 Electric power transmission system model

The electric power transmission system model comprises three parts, the GTA cable, the isolation transformer and the EM railgun cable, each of which will be discussed in the following sections. The equivalent circuit diagram for the electric power transmission system is shown in Figure 4-16. The impedance of the GTA cable, isolation transformer and the EM railgun cable represent the impedance of the electric power transmission system. In addition to the generator reactance the reactance of the isolation transformer and power cables constitute the six-pulse rectifier commutation reactance which as described in section 3.4.1, is crucial in limiting the magnitude of the line to line short circuit currents that may occur during commutation overlap. Accurate modelling of the commutation reactance is also required to enable an accurate representation of the harmonic waveform distortion resulting from the six-pulse thyristor rectifier. In this case an isolation transformer acts to increase the source impedance, as per the approach taken by Sudhoff, et al., (2004) and Steurer, et al., (2007). Increasing the source impedance, thus the commutation reactance, can help to reduce the harmonic waveform distortion resulting from the six-pulse thyristor rectifier by reducing the \(di/dt\), as described in section 3.4.1.

![Figure 4-16 Electric power transmission system equivalent circuit](image-url)

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4.7.1 Generator and EM railgun cable model

The generator cable and EM railgun cable utilise identical models and as such are discussed as one in this section. Each cable is 50 m in length, which allows for the GTA to be installed mid-ship, with the EM railgun installed towards the bow in a candidate warship approximately 200 m in length (Association of the United States Navy, 2015). Furthermore, the total cable length of 100 m is commensurate with that employed by Kanellos, et al., (2007) and Tsekouras, et al., (2010). The model consists of a per phase impedance consisting of resistance per phase $R_c$ and inductance per phase $L_c$ where:

$$R_c \text{ is } 1.08 \text{ m}\Omega \text{ per phase per } 50 \text{ m cable;} \quad L_c \text{ is } 2.7 \text{ uH per phase per } 50 \text{ m cable.}$$

The values of $R_c$ and $L_c$ were calculated based on the cable data sheet provided by NexansAmerCable (2015), the full calculations for which can be found at Appendix G.

4.7.2 Isolation transformer model

The model of the isolation transformer utilises the SimPowerSystems blockset model of a three phase linear transformer as per the equivalent circuit shown in Figure 4-16. This model is a simplification of a three phase transformer and consists of three single phase transformers. A full description of the transformer model is offered by TheMathWorks, Inc. (2014 f). The transformer is connected in a delta-delta (Dd0) configuration thus implements no phase shift between the primary and secondary voltages. The justification for selecting this type of transformer is that the Dd0 transformer connection prevents zero sequence harmonics which originate from the six-pulse thyristor rectifier from propagating into the AC power system (Wakileh, 2001 b). This is because the zero sequence equivalent circuit impedance of the Dd0 connected transformer is open circuit, hence zero sequence harmonics are trapped circulating in the secondary delta windings of the isolation transformer (Wakileh, 2001 b). As HV marine electric power systems are generally earthed, failure to implement a Dd0 isolation transformer between the harmonic source and the AC power system would allow common mode zero sequence currents to circulate through the earth connection (Bucknall, 2007 a).

As shown in Figure 4-16 the transformer model takes into account the primary and secondary winding resistances ($R_1$ and $R_2$) and the primary and secondary winding inductances ($L_1$ and $L_2$). The model also takes into account the magnetizing characteristics of the core which are modelled by a linear impedance branch ($R_m$ and $L_m$). Transformers can be accurately represented by means of the lumped parameters described here and the resulting models are valid in the kHz frequency range (Barret, et al., 1997). As such, the model described here is considered appropriate for use in this research.

**Transformer parameters**

$V_1$ and $V_2$ are the primary and secondary winding voltages (kV); $R_1$ and $R_2$ are the primary and secondary winding resistances (pu); $L_1$ and $L_2$ are the primary and secondary winding inductances (pu); $R_m$ and $L_m$ are the magnetising branch resistance and inductance (pu).
The transformer parameters were selected based on those offered by Kundur (1994 i), who details the per unit characteristics of a 42 MVA, 60 Hz transformer which are given in Table 4-6. All the equivalent circuit values given in Table 4-6 are referred to the primary. This rating of transformer was selected to match the rating of the GTA as closely as possible. As the rating of the transformer offered by Kundur (1994 i) closely matches the rating of the GTA employment of these parameters was considered reasonable. An equivalence in ratings between the GTA and the transformer was sought because the EM railgun circuit may draw the maximum power permitted by the GTA. Hence the isolation transformer, which is connected in series with the GTA and the six-pulse thyristor rectifier ESD charging circuit, must be rated to withstand the full power of the GTA.

<table>
<thead>
<tr>
<th>Model parameter</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transformer rating ($P_n$)</td>
<td>42 MVA</td>
<td>Secondary winding resistance ($R_2$)</td>
<td>0.004 pu</td>
</tr>
<tr>
<td>Transformer primary voltage ($V_1$)</td>
<td>11 kV</td>
<td>Secondary winding inductance ($L_2$)</td>
<td>0.1 pu</td>
</tr>
<tr>
<td>Transformer secondary voltage ($V_2$)</td>
<td>11 kV</td>
<td>Magnetising branch resistance ($R_m$)</td>
<td>500 pu</td>
</tr>
<tr>
<td>Primary winding resistance ($R_1$)</td>
<td>0.004 pu</td>
<td>Magnetising branch inductance ($L_m$)</td>
<td>500 pu</td>
</tr>
<tr>
<td>Primary winding inductance ($L_1$)</td>
<td>0.1 pu</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

### 4.7.3 Electric power transmission system model limitations

The electric power transmission system has the following limitations:

1. It is acknowledged that the model of the transformer is a quasi-steady state impedance model. This representation is considered appropriate for use in this research (Barret, et al., 1997).

2. The transformer model consists of three single phase transformers hence does not take into account the magnetic coupling between the windings of different phases.

3. As the cable model is considered a short transmission line, the requirement for which is that the line be less than 80 km in length, the shunt capacitance is negligible and the cable can be represented by the series impedance $R_c$ and $L_c$ only (Kundur, 1994 j). Hence, the cable models consist of line impedance only and do not take into account the effect of capacitance to earth which is considered appropriate given the short length of cable. This assumption is commensurate with the approach taken by Tsekouras, et al., (2010) when conducting research in this field.

### 4.8 Validation and verification

When conducting simulation based research thorough validation and verification of the models employed is critical in ensuring that the results of the simulations are suitably representative of the real world system they intend to simulate. As such, this section presents the validation process conducted for the GTA model.
and the verification process conducted for the ESD charging circuit model, thus assuring the credibility of
the results.

4.8.1 GTA model validation

When seeking to validate the model of the GTA two key aspects pertaining to the nature of this research
were considered. These key aspects were the validation of the GTA governor response and the validation
of the GTA AVR response, both of which are presented in this section. The validation of the governor
response will be dealt with first.

Governor response validation

The method used to validate the response of the GTA governor was to compare the delivered power and
frequency response during a series of tests with that of the original GT engine model as supplied by Rolls-
Royce. As confirmed in Appendix E, the original GT engine model governor performance was validated
against actual test bed results involving typical load steps and one case of a full loss of electrical load from
36 MW. Hence comparison of the performance tool GTA model’s delivered power and speed against that
of the original GT engine model will be employed as a method to validate the performance tool GTA model
governor, which is responsible for controlling real power and speed.

In validating the governor response two tests were conducted. In the first instance load steps were applied
at 20 s intervals from 2 MW up to the full rated power of 36 MW. 2 MW was selected as the starting point
as this represents the 2 MW EM railgun support load, which is the minimum load the GTA will supply in
this research. In total 4 load steps were applied, the results of which are given in Figure H-16 and Figure
H-17 at Appendix H, which show the delivered power and speed response respectively. In each case the
maximum error occurring during the transient period and the error at steady state between the models’
developed power and speed was recorded in Table 4-7. In the second instance a ramp load from 2 MW to
full rated power of 36 MW was applied over 20 s. Full power was then maintained for 20 s before ramping
down to 2 MW, again over 20 s. The results of the ramp test are shown in Figure H-18 and Figure H-19 at
Appendix H which show the delivered power and speed response respectively. For this case as the error
was variable over the ramp time the maximum and minimum errors during the ramp up and ramp down
were recorded in Table 4-7.
As shown in Table 4-7 the model performs better at higher loads than at lower loads. At 2 MW load, the steady state error in the developed power is 11.25%, which is relatively high when compared with the errors at increased loads. Once the load is greater than 9 MW the transient developed power errors are all below 4.50%, while the steady state developed power errors are below 2.50%. During the ramp up in load the delivered power error exhibits a steady decrease from 12.50% to 3.00% when ramping from 2 MW to 36 MW respectively, as shown in Figure H-18. During the ramp down in load the delivered power error increased from -4.50% to 12.50%, from 36 MW to 2 MW respectively.

Throughout both the step load and ramp load testing the frequency error was very low. As shown in Table 4-7 the largest frequency error is 1.85% which occurs during the 2 MW to 9 MW step load transient period. Unlike the developed power error the frequency error does not exhibit any appreciable decline under increasing load and remains acceptably low across the whole GTA load range. This high level of agreement between the MT30 GT model frequency and the GTA model frequency can be seen in Figure H-17 and Figure H-19 in Appendix H. The evidence presented in Table 4-7 and Appendix H demonstrate a high level of agreement between the original MT30 GT model governor response and that of the upgraded GTA model developed in this research, across the scenarios examined. However, based on the results presented in Table 4-7 it is not recommended that the GTA model be run with a load of less than 2 MW, as below this point the model validity cannot be guaranteed within an accuracy of 11.25%. As 2 MW represents the minimum load required the model was considered valid for use in this research.

**AVR response validation**

As no real world test data against which to validate the response of the AVR was available an alternative approach was adopted. This approach involved testing the AVR against the Lloyd’s Register generator control testing procedure described by Lloyd’s Register (2015) which takes into account section 9.4 generator control. As this method is employed as an acceptance test for AVRs in the real world, testing the response of the AVR model against the Lloyd’s Register criteria can be considered a valid approach. The testing comprises measuring maximum voltage variation at 25%, 50%, 75% and 100% load as well as measuring voltage rise when rejecting 25% load at 0.8 PF from 25%, 50%, 75% and 100% load. The results
of the testing are shown in Figure H-20 and Figure H-21 at Appendix H, while a summary is given in Table 4-8.

### Table 4-8 GTA AVR model validation measurements

<table>
<thead>
<tr>
<th>Test description</th>
<th>Criteria</th>
<th>Measured</th>
</tr>
</thead>
<tbody>
<tr>
<td>25% load</td>
<td>Voltage within ± 2.50%</td>
<td>-0.09%</td>
</tr>
<tr>
<td>50% load</td>
<td>Voltage within ± 2.50%</td>
<td>-0.09%</td>
</tr>
<tr>
<td>75% load</td>
<td>Voltage within ± 2.50%</td>
<td>-0.09%</td>
</tr>
<tr>
<td>100% load</td>
<td>Voltage within ± 2.50%</td>
<td>-0.09%</td>
</tr>
<tr>
<td>25% load – reject 25% load at 0.8 pf</td>
<td>Voltage rise &lt; 7.50%</td>
<td>5.54%</td>
</tr>
<tr>
<td>50% load – reject 25% load at 0.8 pf</td>
<td>Voltage rise &lt; 7.50%</td>
<td>5.00%</td>
</tr>
<tr>
<td>75% load – reject 25% load at 0.8 pf</td>
<td>Voltage rise &lt; 7.50%</td>
<td>4.72%</td>
</tr>
<tr>
<td>100% load – reject 25% load at 0.8 pf</td>
<td>Voltage rise &lt; 7.50%</td>
<td>4.54%</td>
</tr>
</tbody>
</table>

As shown in Table 4-8 the AVR model satisfies all the criteria for the tests undertaken and thus can be considered an acceptable model of an AVR suitable for employment in a warship electric power system.

In addition to validating the response of the GTA speed governor and GTA AVR a transient situation was simulated and the response compared with the theory discussed in section 3.3.1. To examine the transient response of the synchronous alternator model a bolted three phase to earth fault was applied to the terminals of the generator for a period of 10 cycles, which for a 60 Hz system corresponds to a time period of 0.16 s. The GTA voltage and current during this transient event are shown in Figure 4-17. As shown in Figure 4-17 the GTA voltage and current response is commensurate with the theoretical result presented at Figure 3-7 in section 3.3.1 with the sub-transient and transient reactance effects clearly visible in the current plot. Once the fault is cleared the AVR responds to recover the voltage. The result of this test increases confidence in the transient response of the GTA and AVR models employed in this research.
4.8.2 EM railgun ESD charging circuit model verification

As the controller employed in this research has been developed specifically for charging the EM railgun ESD no real world test data against which to validate the control system exists, nor do any pre-defined standards against which to measure acceptability. As such, the control system was verified across a ramp charge scenario to ensure the response of the controller was as intended. This is considered verification.

**EM railgun ESD ramp charge control method**

The ESD ramp charge control method involved charging the ESD from empty to its fully charged voltage of 11 kV by applying a ramp charge demand at a constant rate over a time period of 20 s starting at time 5 s. The results of the ramp charge control test are shown below in Figure 4-18 which shows the ESD voltage and ESD voltage set point in the upper plot, the firing delay angle $\alpha$ in the middle plot and the GTA real power in the lower plot.
As shown in Figure 4-18 the ESD voltage follows the ESD voltage set point very closely. In this case, as the charge demand is a continuous ramp the firing angle exhibits a constant decrease as the charge demand continually increases, as described in section 4.5.2. This is reflected in the bottom plot, which demonstrates a constant ramp up of the GTA real power as the charge demand increases.

The results presented in Figure 4-18 of this section have demonstrated that the EM railgun charging circuit and controller developed for use in this research are capable of accurately charging an EM railgun ESD in response to a ramp charge demand. Also, the controller behaves as expected when considered against the design principles set out in section 4.5.2. Hence, the suggested EM railgun charging control circuit was considered to have achieved an acceptable level of verification for use in this research. The ramp charge method is commensurate with the approach taken by Lewis (2006) when conducting research into charging an EM railgun ESD, the results of which were reviewed in Chapter 2.
4.8.3 EM railgun ESD model verification

As stated in section 4.6.1 the capacitors selected on which to model the ESD are based on those presented by Wolfe, et al., (2005). These capacitor modelling parameters have been validated against actual experimental EM railgun shot data and have been proved capable of powering EM railguns, the evidence for which is presented by McNab, et al., (1995) and Wolfe, et al., (2001). To ensure the ESD model based on these characteristics behaved as expected in this simulation, verification was conducted. To verify the ESD model the ESD was charged to its fully charged voltage of 11 kV over 10 s, during which the stored energy was recorded. The stored energy during the charge was calculated using Equation 38. The results are shown in Figure 4-19.

\[
ESD \text{ stored energy} = \int_{T=5}^{T=15} (V_{\text{Cap}} \cdot I_{\text{Cap}}) dt
\]

Where \( V_{\text{Cap}} \) is the ESD capacitor charge voltage (V); \( I_{\text{Cap}} \) is the ESD capacitor charging current (A).

As shown in the upper plot of Figure 4-19 the ESD is charged to its fully charged voltage of 11 kV in 10 s. The charging current is constant as shown in the middle plot. During this time 160 MJ is delivered to the ESD, as shown in the lower plot. This is commensurate with the characteristics described in section 4.6.1. Thus, the ESD model is considered verified for use in this research.

![ESD capacitor verification plot](image)
4.8.4 Electric power transmission system model verification

The models of the isolation transformer and power cables are based on parameters provided by NexansAmerCable (2015) and Kundur (1994 i) respectively. The isolation transformer parameters given by Kundur (1994 i) are based on an example of a 42 MVA, 60 Hz transformer, while the power cable parameters provided by NexansAmerCable (2015) are based on manufacturer's data of an actual power cable suitable for this application, both of which are considered acceptable sources. The models employed to represent these components are all composed of SimPowerSystems blockset library models with no further modifications. As the values used parametrise these models are considered to be from acceptable sources and considering the relative simplicity of the models, further verification was not considered necessary.

4.9 Summary

This section has described the performance tool constructed to conduct simulation based research into the transient and harmonic impact of an EM railgun and its associated charging system on the QPS of a candidate warship electric power system. Firstly, to facilitate the integration of highly relevant models, provide continuity with previous research in this field and to align with the industry standard for the modelling of warship electric power systems, MATLAB®/Simulink® was selected as the software package with which to conduct this simulation based research. The modelling constraints and assumptions adhered to throughout this research were also stated.

This chapter then described the modelling requirement to simulate the system of interest in this research without unnecessarily increasing the simulation complexity or run-time. It was concluded that the models required were a 36 MW GTA with associated AVR and governor control systems, power transmission cables, an isolation transformer, the EM railgun ESD charging circuit, the EM railgun ESD and the 2 MW EM railgun support load. A description of each of the previously mentioned models was then offered. The model schematic or equivalent circuit, a justification of and selection of the model parameters and the model limitations were described in each case, where appropriate.

To ensure that the results of simulations were suitably representative of the candidate warship electric power system they intend to simulate a thorough validation and verification process was conducted. It was concluded that each model is suitable for use in this research and is capable, where appropriate, of accurately representing the transient and harmonic characteristics of the warship electric power system when providing power for an EM railgun as theoretically described in Chapter 3, thus assuring the credibility of the results. A summary of each of the models’ validity and limitations is given below in Table 4-9. A brief overview of the performance tools operation is offered at Appendix I, to equip the reader with an understanding of how the performance tool has been used to conduct this research. The results required to analyse the transient and harmonic impact of EM railguns on warship electric power system QPS, generated by the model described in this chapter, are presented in Chapter 5.
### Table 4-9 Performance tool modelling summary

<table>
<thead>
<tr>
<th>Model</th>
<th>Validity</th>
<th>Limitations</th>
</tr>
</thead>
<tbody>
<tr>
<td>GTA</td>
<td>Validated</td>
<td>Does not include a thermodynamic representation of the compressors and turbines. Does not simulate start-up, shut down or idle. Load sheds greater than 18 MW must be applied over a time period greater than 20 ms.</td>
</tr>
<tr>
<td>EM railgun ESD charging circuit</td>
<td>Verified</td>
<td>Controller can execute pre-defined shot patterns only.</td>
</tr>
<tr>
<td>EM railgun 160 MJ ESD</td>
<td>Verified</td>
<td>The capacitor model only accounts for the series resistance and does not take into account the equivalent leakage resistance.</td>
</tr>
<tr>
<td>Power cables and isolation transformer</td>
<td>Verified</td>
<td>The isolation transformer model is a quasi-steady state impedance model. The cable models consist of line impedance only and do not take into account the effect of shunt capacitance to earth.</td>
</tr>
</tbody>
</table>
Chapter 5 Results

5.1 Introduction

So far this thesis has introduced and described the question considered in this research, which is what are the implications and consequences of integrating an EM railgun with a warship electric power system and under what constraints should the EM railgun be operated to ensure an acceptable level of QPS to other electrical consumers during firing operations. It was analytically demonstrated in Chapter 3 that the EM railgun will have a transient and harmonic impact on warship electric power system QPS. Chapter 3 also described the GTA control systems responsible for maintaining the voltage and frequency elements of QPS, before suggesting that a six-pulse thyristor rectifier be employed to regulate the ESD charging cycle. Owing to the characteristics of the six-pulse thyristor rectifier, it was analytically demonstrated that the firing delay angle $\alpha$ will not only dictate the rate of charge of the ESD, and as such may be used to limit the power system transients, but will also introduce harmonic waveform distortion into the electric power system which will impact on the alternator power factor over the ESD charging cycle.

To address the research question it was suggested that time-domain simulations be implemented as a tool to investigate the previously discussed impacts of EM railgun operations on QPS. As such, Chapter 4 described the development of a performance tool constructed in MATLAB®/Simulink®, capable of simulating the transient and harmonic behaviour of a candidate warship electric power system subject to EM railgun operations when employing the ESD charging control system described in Chapter 3. EM railgun operations may include firing a single shot as well continuously firing multiple shots in succession at a rate of 10 – 12 shots per minute (Chaboki, et al., 2004) (Osborn, 2015). Depending on the tactical situation, both ship defence and shore bombardment may require both single shots and continuous firing. Single shots may be required to fire warning shots or target specific enemy positions, while continuous firing may be required to provide multiple rounds on target when providing naval fire support, or to increase the chances of kill when prosecuting high speed missile and surface threats. Hence, this chapter will present the results of simulation based research conducted into the transient and harmonic impact of EM railgun operations on QPS when conducting both single shot and continuous firing operations. In each case the justification for and a description of the investigation undertaken is given before the results and
observations are presented. The aim of the investigation is to explore the impact of single shot and continuous firing on QPS and to establish the EM railgun firing limits in each case.

5.2 Investigation justification

The tactical situation under which the EM railgun is being employed may require both single shots and continuous firing. The justification for firing single shots is the requirement to fire warning shots or to target specific enemy positions. The justification for continuous firing is the requirement to provide naval fire support to forces shore, or to increase the chances of kill when prosecuting high speed missile and surface threats. As such, this investigation is considered in two parts which reflect the two distinct firing protocols, being single shot and continuous firing. In both parts the aim of the investigation is to investigate the impact of the firing operation on the transient and harmonic elements of QPS described in section 3.5 and to explore the firing constraints which may impact on the operational capability of the EM railgun. The justification for conducting this investigation is to obtain results which demonstrate the transient and harmonic impact of EM railgun operations on QPS. These results will then be used to support and develop recommendations for the naval design authority when integrating EM railguns with warship electric power systems. Furthermore, the aim of exploring the EM railgun firing constraints is to obtain a set of results from which the EM railgun firing limits can be defined, from a QPS perspective. The justification for this is to inform the operator of the EM railgun firing constraints which must be adhered to such that an acceptable level of QPS is maintained to other consumers which may be exposed to the transient and harmonic impact of EM railgun operations.

For the case of single shot firing the aim is to determine the minimum time in which a single shot can be taken. This effectively informs the operator of the minimum time required between requesting a shot and being able to fire the weapon, whilst maintaining an acceptable standard of QPS. A complete single shot cycle comprises the time taken to charge the ESD with the energy required for one shot and the time taken to unload the GTA and return the power system to steady state following the shot. For the case of the system shown in Figure 3-9 steady state operation means supplying the 2 MW EM railgun support load only. This information is useful to the naval design authority when considering the capability of the weapon against the impact on QPS. For the case of continuous firing the aim is to determine the minimum time within which the GTA can continuously charge the ESD with 160 MJ in between the firing of shots, whilst the power system remains within a state of dynamic stability. A described by Kundur (1994 e) the power system should be able to remain in a state of operating equilibrium under normal operating conditions and to regain an acceptable state of equilibrium following a transient disturbance. Hence, during the operation of the EM railgun the power system should achieve a state of stability whereby transients are repeatable and no signs of voltage or frequency deterioration are exhibited thus, the system is in equilibrium and considered dynamically stable. Furthermore following the firing operation the system must return to steady state, thus regaining an acceptable state of equilibrium. Defining the minimum time within which the GTA can continuously charge the ESD with 160 MJ in between the firing of shots essentially defines the maximum rate of continuous firing. This rate of fire limit is important to the naval design authority when assessing the capability of the weapon against the impact on QPS.
For the case of both single shot and continuous firing the investigation is conducted using the performance tool described in Chapter 4. To conduct the investigation the rate of charge of the ESD is varied using the EM railgun ESD charging circuit model described in section 4.5. The EM railgun ESD charging circuit controls the six-pulse thyristor rectifier firing delay angle (α) which controls the ESD charging current (I\text{charge}), as shown in Figure 5-1. This process was described in detail in section 3.4.1 and 3.4.2 in Chapter 3. Varying the rate of charge of the ESD allows the simulation of varying single shot times and varying continuous rates of fire, from which the impact on QPS can be explored.

![Figure 5-1 Performance tool system diagram – EM railgun single shot and continuous firing investigation circuit](image)

A summary of the tests undertaken and the results sought from this research is presented in Table 5-1. A detailed description of the tests undertaken for both single shot and continuous firing which includes the justification for the test boundaries is offered in sections 5.3.1 and 5.4.1 respectively. For the case of continuous firing an additional test was undertaken to assess the extent to which retaining residual charge in the ESD capacitor can limit the GTA load transients during continuous firing operations. While this test is captured in Table 5-1 a full description is offered in section 5.4.2.

In addition to the results sought as summarised in Table 5-1 single shot and continuous firing case studies were also undertaken. The aim of the case studies was to allow further exploration of the system response by recording a wider range of results, including the harmonic impact on QPS. This range of results would not be practical to record over the range of tests shown in Table 5-1. A more detailed explanation of the single shot and continuous firing case studies is offered in sections 5.3.3 and 5.4.3 respectively, while a summary of the results sought is offered in Table 5-2. Furthermore, obtaining the additional results detailed in Table 5-2 allows the results of the investigation to be related to the theoretical analysis of the research problem presented in Chapter 3. This allows a more complete understanding of the research problem considered to be developed.
Table 5-1 Summary of research tests and results sought

<table>
<thead>
<tr>
<th>Test</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>ESD charge time</td>
<td>Simulate the charging of the ESD with 160 MJ within times ranging from 7 s to 10 s in 0.25 s increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
<tr>
<td>GTA unload</td>
<td>Simulate ramping the GTA from 40 MW to 2 MW thus returning the power system to steady state following a shot. Simulate ramp times ranging from 2 to 10 s in 0.50 s increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
<tr>
<td>GTA load shed</td>
<td>Simulate GTA load sheds from 40 MW ranging from 1 to 13 MW in 1 MW increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
<tr>
<td>ESD recharge</td>
<td>Simulate the firing of six continuous shots recharging the ESD with 160 MJ between each shot within times ranging from 4.25 s to 5 s in 0.25 s increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
</tbody>
</table>

Continuous firing

<table>
<thead>
<tr>
<th>Test</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>GTA load shed</td>
<td>Simulate GTA load sheds from 40 MW ranging from 1 to 13 MW in 1 MW increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
<tr>
<td>ESD recharge</td>
<td>Simulate the firing of six continuous shots recharging the ESD with 160 MJ between each shot within times ranging from 4.25 s to 5 s in 0.25 s increments.</td>
<td>GTA voltage deviation (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>GTA frequency deviation (%)</td>
</tr>
</tbody>
</table>

Table 5-2 Summary of research tests and results sought from single shot and continuous firing case studies

<table>
<thead>
<tr>
<th>Test</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>Single shot firing case study</td>
<td>Simulate the firing cycle for a single shot in its entirety. Select a single ESD charge time and GTA unload time based on the results of the investigation conducted in section 5.3.2.</td>
<td>GTA RMS current&lt;br&gt;GTA RMS voltage&lt;br&gt;GTA frequency&lt;br&gt;GTA apparent power&lt;br&gt;GTA real power&lt;br&gt;GT A reactive power&lt;br&gt;Firing delay angle&lt;br&gt;GTA power factor&lt;br&gt;Main bus THD</td>
</tr>
</tbody>
</table>

Continuous firing

<table>
<thead>
<tr>
<th>Test</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>Continuous firing case study</td>
<td>Simulate the firing cycle for six continuous shots in its entirety. Select a single ESD charge time and GTA unload time based on the results of the investigation conducted in section 5.3.2. Select ESD capacity and time between shots based on the results of the investigation conducted in section 5.4.2.</td>
<td>GTA RMS current&lt;br&gt;GTA RMS voltage&lt;br&gt;GTA frequency&lt;br&gt;GTA apparent power&lt;br&gt;GTA real power&lt;br&gt;GT A reactive power&lt;br&gt;Firing delay angle&lt;br&gt;GTA power factor&lt;br&gt;Main bus THD</td>
</tr>
</tbody>
</table>

5.3 Single shot firing

This section will present the results of research conducted into the impact of firing single EM railgun shots on QPS. The aim of this section is to demonstrate the minimum time within which a single shot can be fired whilst maintaining an acceptable level of QPS as defined by NATO STANAG 1008. Following this a single
shot test case will be simulated and the power system performance characteristics presented in greater detail to allow more in depth analysis of the results.

5.3.1 Investigation description

For the purposes of this investigation the EM railgun firing cycle will be considered a two stage process. The first stage comprises the ESD charge time whereby following acquisition of the target at time zero, the required 160 MJ shot energy is transferred to the ESD. This first stage is shown in Figure 5-2, which shows the capacitor voltage increasing from 0 to the fully charged voltage of 11 kV in the upper plot and the GTA power, which begins the charging stage from supplying only the 2 MW EM railgun support load, attaining power $x$ in the lower plot. The rate of charge of the ESD and thus the GTA power required $x$, will depend on the ESD charge time thus will define the rate at which energy is supplied to the ESD. Following this stage the shot would be taken, the time for which is assumed negligible.

![Figure 5-2 GTA power and ESD voltage against time during single shot operation](image)

The second stage pertains to the requirement for the GTA to unload on completion of the ESD charging phase to return the power system to steady state. This will also impact on QPS due to the load change following the shot. As shown in Figure 5-2 during the ESD charging stage the GTA will attain power $x$. Once the charging of the ESD is complete and the shot has been fired the balance between generation and load must be maintained, with failure to do so resulting in the GTA shedding load which would result in the intermittent loss of GTA power. To avoid this, the charging bridge is employed to divert the GTA power into the brake resistor, shown in Figure 4-15 in section 4.6. The charging bridge control system then reduces the load on the GTA to match the 2 MW EM railgun support load, by facilitating a controlled decrease in output power. This is achieved by increasing the charging bridge firing angle from its final value when charging the ESD to 90 degrees, at which point the mean output voltage is zero. The rate of GTA unload is defined by the unload time, as shown in Figure 5-2. Hence, a single shot comprises the full firing cycle.
shown in Figure 5-2. The results presented in this section are the results sought as per the tests identified in section 5.2 and summarised in Table 5-1 for the case of single shot firing.

5.3.2 Single shot firing investigation results

To investigate the impact of single shot firing on QPS the charging of the ESD was simulated over a range of charge times and the unloading of the GTA was simulated over a range of unload times, thus separately simulating stage 1 and 2 of the firing cycle shown in Figure 5-2. The reason for investigating the ESD charge time and GTA unload time separately was to ascertain the impact on QPS pertaining to each stage of the firing cycle. Once this is known, any ESD charge time can be coupled with any GTA unload time to achieve the level of QPS required.

To conduct an investigation of the ESD charge time the charging period increased from 7 s to 10 s at 0.25 s intervals. In each case the maximum frequency and voltage deviations were recorded, the results of which are shown in Figure 5-3 and Figure 5-4 respectively. The full results can be found at Appendix J. As shown in Figure 5-3 the characteristic of the curve show the GTA frequency rapidly deteriorating below the NATO STANAG 1008 maximum transient limit towards a charge time of 7 s. For this reason investigation did not continue below an ESD charge time of 7 s since the GT begins to stall.

Figure 5-3 GTA frequency deviation against 160 MJ ESD charge time

Figure 5-3 shows the maximum frequency deviation on the y axis plotted against the corresponding charge time on the x axis. Also shown on the plot are the NATO STANAG 1008 QPS frequency limits for tolerance, transient tolerance and maximum transient tolerance.

Figure 5-3 demonstrates that to maintain QPS within the maximum transient tolerance or the transient tolerance, the minimum ESD charge time must be 8 s or 8.38 s respectively. Figure 5-3 also shows that when the ESD charge time is beyond 8.75 s the frequency deviation will be within tolerance.
Figure 5-4 GTA voltage deviation against 160 MJ ESD charge time

Figure 5-4 shows the maximum voltage deviation on the y axis plotted against the corresponding charge time on the x axis. Also shown on the plot are the NATO STANAG 1008 QPS voltage limits for transient tolerance and maximum transient tolerance.

Figure 5-4 demonstrates that to maintain QPS within the maximum transient tolerance or the transient tolerance, the minimum ESD charge time must be 8.25 s or 9.77 s respectively. Investigation did not continue beyond 10 s as the characteristic of the curve plotted in Figure 5-4 suggests that maintaining the voltage deviation within the tolerance limit would require an unreasonably long charge time. Nonetheless, two of the QPS criteria were satisfied within the range of charge times tested.

Following this, the impact of the GTA unload time on QPS was examined. To conduct this investigation the GTA unload time was increased from 2 s to 10 s at 0.50 s intervals. In each case the maximum frequency and voltage deviations were recorded, the results of which are shown in Figure 5-5 and Figure 5-6 respectively. The full results can be found at Appendix K.
Results

Figure 5-5 GTA frequency deviation against GTA unload ramp time

Figure 5-5 shows the maximum frequency deviation on the y axis plotted against the corresponding GTA unload ramp time on the x axis. Also shown on the plot are the NATO STANAG 1008 QPS frequency limits for tolerance, transient tolerance and maximum transient tolerance.

Figure 5-5 demonstrates that to remain within the maximum transient tolerance limit the minimum unload time must be 2.25 s. To remain within the transient tolerance limit the minimum unload time must be 3 s, while unloading in a minimum time of 4.25 s complies with the tolerance limit. The characteristic of the curve plotted suggests that attempting to unload in less than 2 s would result in rapidly increasing the magnitude of the frequency deviation outside the NATO STANAG 1008 maximum transient tolerance limit, hence investigation did not continue below an unload time of 2 s.

Figure 5-6 GTA voltage deviation against GTA unload ramp time
Figure 5-6 shows the maximum voltage deviation on the y axis plotted against the corresponding GTA unload ramp time on the x axis. Also shown on the plot is the NATO STANAG 1008 QPS voltage tolerance limit. Figure 5-6 demonstrates that across the full range of unload times examined the voltage deviation remained within the tolerance limit. Furthermore it was shown that beyond an unload time of 8 s the voltage deviation demonstrated a constant deviation of 0.35 %.

5.3.3 Single shot case study

To examine the response of the system further and to enable comparison with the results presented in the previous sections a single shot case study was simulated in its entirety. To facilitate this a single ESD charge time and GTA unload time were selected based on the results presented in section 5.3.2. The ESD charge time was set at 8.75 s which suggests that the voltage deviation will remain within the maximum transient tolerance limit and the frequency deviation will remain within tolerance limit. The GTA unload time was set to 4.50 s which suggests that both the voltage and frequency deviation will remain within tolerance. While it is acknowledged that the GTA can unload more quickly than this it was deemed reasonable to exploit the initial steep section of the curve which suggests that QPS will remain within tolerance for a relatively quick GTA unload time, thus minimising the impact on the GTA.

A complete firing cycle based on the previously stated ESD charge time and GTA unload time was then simulated, full system characteristic results of which are shown in Figure 5-7, Figure 5-8 and Figure 5-9. The ESD charging begins at 5 s which allows the simulation to be at steady state beforehand. The ESD charging stage is complete at 13.75 s at which point the shot would be taken. The GTA then unloads which is complete by 18.25 s.

![Figure 5-7 GTA RMS current, RMS voltage and frequency during single shot operation](image)
Results

Figure 5-7 shows the GTA RMS current, GTA RMS voltage and GTA frequency on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure 5-7 shows that upon commencement of ESD charging there is an immediate demand for charging current which rises immediately from 250 A to 2.75 kA. The GTA RMS voltage dips to 9 kV (-18.50%) which is within the maximum transient tolerance as defined by NATO STANAG 1008. The GTA frequency dips to 58.20 Hz (-3.00%) which is within the transient tolerance as defined by NATO STANAG 1008.

The GTA RMS voltage then recovers to 11 kV within 2 s and the GTA frequency recovers to 59 Hz. The voltage and frequency then remain at steady state throughout the charging period. As the frequency remains within tolerance the recovery time can be negated. During the GTA unload Figure 5-7 shows the GTA RMS voltage rising to 11.20 kV (1.80%) which is within tolerance and the GTA frequency increasing to 61.50 Hz (2.50%) which is also within tolerance as defined by NATO STANAG 1008. As the voltage and frequency deviations remain within tolerance during the GTA unload the recovery time can be negated.

Figure 5-8 GTA apparent, real and reactive power during single shot operation

Figure 5-8 shows the GTA apparent power, real power and reactive power on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure 5-8 demonstrates that upon commencement of ESD charging the GTA apparent power increases to 60 MVA and remains between 50 and 60 MVA throughout the charging period. The GTA real power exhibits a linear increase during the charging period increasing from the 2 MW base load to 40 MW, which represents a temporary 110% overload of the GTA. The GTA reactive power increases to 55 MVAr then decreases to 40 MVAr.
Results

throughout the ESD charging period. During the GTA unload the apparent, real and reactive power all
decrease back to match the 2 MW EM railgun support load.

From the observations drawn from Figure 5-8 conclusions on the composition of the charging current
shown in Figure 5-7 can be reached. Consider first, that the GTA RMS current shown in Figure 5-7 and the
GTA apparent power shown in Figure 5-8 share the same profile.

The apparent power (S), is composed of real power (P) and reactive power (Q) thus:

\[ S^2 = P^2 + Q^2 \]  \[\text{[E39]}\]

Hence, it follows that

\[ I^2 = I_p^2 + I_Q^2 \]  \[\text{[E40]}\]

The real and reactive power components correspond to the components of the real and reactive current
drawn from the generator. Hence, when observing Figure 5-8 it can be concluded that during the early
stages of the ESD charging the charging current is predominantly reactive and towards the end of the
charging cycle is equally real and reactive. As the GTA governor controls the real power flow, the
magnitude of the frequency transient corresponds to the real current component of the charging current.

Thus, the ESD is charged with a combination of real and reactive current which helps to facilitate a rapid
charge time, whilst maintaining the GTA frequency within QPS limits as defined by NATO STANAG 1008
by limiting the real current. This result is commensurate with the analytical analysis offered in section 3.4.1
and 3.4.2 thus supports the verification of this result. As described in 3.4.2, the composition of the charging
current depends on the firing delay angle \( \alpha \) of the thyristor charging bridge which combined with the power
drawn at harmonic frequencies, also impacts on the GTA power factor. Results demonstrating this are
shown in Figure 5-9.
Figure 5-9 Charging bridge firing delay angle, GTA power factor and main bus THD during single shot operation

Figure 5-9 shows the charging bridge firing delay angle, the GTA power factor and the main bus THD on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis.

Figure 5-9 shows that during the ESD charging stage the charging bridge firing delay angle decreases linearly from 90 degrees to 25 degrees within the 8.75 s charge time. The firing delay angle then increases from 25 degrees to 90 degrees within the 4.50 s GTA unload time. Commensurate with the observations drawn from Figure 5-8 the GTA power factor decreases from 0.75 lagging to 0.15 lagging immediately as charging commences, increasing linearly throughout the charging cycle to 0.75 lagging. The power factor then decreases to 0.35 lagging during the GTA unload before returning to 0.75 lagging once the GTA unload is complete. This result is supported by Equation 16 presented in section 3.4.1. Upon commencement of ESD charging the main bus THD increases immediately from 2.50% to 30%, before decreasing linearly to 22.50% throughout the charging cycle. As the GTA unloads the THD briefly increases to 25% before decreasing to 2.50% once the GTA unload is complete. This level of THD is outside NATO STANAG 1008 QPS limits, the implications and consequences of which will be discussed in Chapter 6.

From the observations drawn from Figure 5-9 it can be concluded that when α is closer to 90 degrees, the harmonic waveform distortion is high. It is also concluded that when α is closer to 90 degrees the power factor is low. This is due in part to the high harmonic power component and to the high phase control reactive power component. The observations drawn from Figure 5-9 are commensurate with the analytical analysis offered in section 3.4.1 and 3.4.2 thus supporting the verification of these results.
Figure 5-10 GTA energy supplied during single EM railgun shot

Figure 5-10 shows the GTA energy supplied during the single EM railgun shot on the y axis, plotted against time on the x axis. The energy flow was measured at the DC side of the ESD charging bridge and does not include the energy supplied to the 2 MW EM railgun support load. Figure 5-10 shows that during the ESD charge stage the GTA delivers 160 MJ to the ESD and that during the GTA unload stage the GTA generates 59 MJ of energy which is dissipated in the ESD brake resistor.

5.3.4 Single shot firing results summary

Based on the results presented in Figure 5-3, Figure 5-4, Figure 5-5 and Figure 5-6 it can be concluded that the transient impact on QPS can be controlled by using the six-pulse thyristor charging bridge to limit the rate of charge of the ESD. To comply with the STANAG 1008 QPS limits during single shot operations constraints must be placed on the ESD charge time and on the GTA unload time. These constraints are summarised in Table 5-3. As shown in Table 5-3, to comply with the transient and maximum transient limits for both voltage and frequency the ESD charge time must be limited to 9.77 s or 8.25 s respectively. This represents the minimum time within which a shot could be taken from the point of target acquisition whilst meeting the NATO STANAG 1008 QPS requirements for a transient and a maximum transient respectively. Table 5-3 also shows the minimum total firing cycle times corresponding to the transient and maximum transient QPS limits as defined by NATO STANAG 1008. The total firing cycle times are 12.77 s and 10.50 s respectively.

<table>
<thead>
<tr>
<th>NATO STANAG 1008 QPS limit compliance</th>
<th>ESD charge time (s)</th>
<th>GTA unload time (s)</th>
<th>Total firing cycle time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum transient</td>
<td>8.25</td>
<td>2.25</td>
<td>10.50</td>
</tr>
<tr>
<td>Transient</td>
<td>9.77</td>
<td>3.00</td>
<td>12.77</td>
</tr>
</tbody>
</table>

The GTA real power plot in Figure 5-8 demonstrates that the ramp rate of the real power delivery is successfully controlled which in turn limits the impact on the GTA frequency transients. From the case study presented it was also shown that in addition to controlling the rate of charge of the ESD the six-pulse
thyrister charging bridge impacts on the alternator power factor. This is because the firing delay angle $\alpha$ used to control the rate of charge of the ESD gives rise to a large phase control reactive power component and introduces a harmonic power component, both of which reduce the power factor. Furthermore, the characteristics of charging bridge introduced a high level of harmonic waveform distortion during the ESD charging which peaked at 30%. This is outside of STANAG 1008 QPS limits, the implications and consequences of which will be discussed in Chapter 6.

A comparison between the predicted impact on QPS presented in Figure 5-3, Figure 5-4, Figure 5-5 and Figure 5-6 and the actual impact on QPS observed when simulating an entire firing cycle is offered in Table 5-4.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Predicted deviation</th>
<th>Actual deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>ESD charge</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Frequency</td>
<td>-3.00%</td>
<td>-3.00%</td>
</tr>
<tr>
<td>Voltage</td>
<td>-18.50%</td>
<td>-18.50%</td>
</tr>
<tr>
<td>GTA unload</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Frequency</td>
<td>3.00%</td>
<td>2.50%</td>
</tr>
<tr>
<td>Voltage</td>
<td>0.75%</td>
<td>1.80%</td>
</tr>
</tbody>
</table>

As shown in Table 5-4 the predicted and actual deviation values during the ESD charge period correlate well. During the GTA unload the frequency deviation was less than expected. This is because the predicted frequency deviation assumed a steady state frequency of 60 Hz before the transient occurred. However as shown in Figure 5-7 the GTA frequency is at 59 Hz when the GTA unload commences, thus introducing an offset into the overall frequency deviation before the GTA unload. The voltage deviation during the GTA unload was not accurately predicted, however the difference was not significant and the voltage deviation remained within tolerance, as expected. It can therefore be concluded that the plots presented in Figure 5-3, Figure 5-4, Figure 5-5 and Figure 5-6 make for a useful reference when seeking to predict the transient impact of EM railguns on QPS when firing single shots.

The results presented thus far have demonstrated that to maintain both voltage and frequency deviations within NATO STANAG 1008 QPS transient limits a minimum ESD charge time of 8.25 s must be allowed from the point of target acquisition. Furthermore, a minimum unload time of 2.25 s when returning the system to steady state following the shot must be accommodated. This would allow a single shot to be fired within a minimum time of 10.50 s while maintaining an acceptable standard of QPS as defined by NATO STANAG 1008.

### 5.4 Continuous firing

This section presents the results of research conducted into the impact of continuous EM railgun firing on QPS. The aim of this section is to demonstrate the maximum continuous rate of fire achievable whilst maintaining an acceptable level of QPS as defined by NATO STANAG 1008. Following cross examination of a previously suggested method to facilitate continuous firing (Chaboki, et al., 2004) a novel method devised to maintain an acceptable level of QPS as defined by NATO STANAG 1008 will be explored.
Following this, a continuous firing test case will be selected and the power system performance characteristics presented in greater detail to allow a more in depth analysis of the results. The rate of fire limits when firing continuous shots will then be explored.

### 5.4.1 Investigation description

As previously described a single shot constitutes the ESD charge time and the GTA unload time, with the power system returning to steady state following the shot. To facilitate continuous firing the aim is to sustain the firing of multiple shots in succession. Thus, a third stage is introduced into the firing cycle during which the GTA continuously recharges the ESD, whilst firing shots at a defined rate. At the end of the continuous firing operation the GTA then unloads. This means that the system will not return to steady state in between shots, but will remain in a transient state. To maintain an acceptable level of QPS during continuous firing the system must be dynamically stable throughout and regain the initial state of equilibrium once the firing operation is complete.

Owing to the fact that the GTA can deliver 38 MW (when at 110% overload and when allowing for the 2 MW EM railgun support load) the GTA should be able to supply the required 160 MJ in approximately 5 s when allowing margin for associated switching and transmission losses. This assumes that the GTA can supply full power for the duration of the charging sequence and maintain this over multiple shots. The premise for conducting continuous firing in this manner is that the ESD is initially charged with 160 MJ, then following the first shot subsequently recharged with 160 MJ whilst maintaining the GTA at full power, as described by Chaboki, et al., (2004). The initial charge of the ESD and the retention of the GTA at full power during recharge is facilitated by the thyristor charging bridge. This suggested continuous firing method is shown in Figure 5-11. Figure 5-11 shows the ESD capacitor voltage in the upper plot and the GTA power in the lower plot.

![Figure 5-11 Suggested continuous firing method](image)
The problem with the method suggested by Chaboki, et al., (2004), is that once a shot is taken and the ESD has been discharged to the EM railgun the capacitor is empty, or perhaps practically empty, as shown in Figure 5-11 immediately after the first shot. As a result when the capacitor is reconnected to the GTA to be recharged an inrush current of three times rated current occurs which causes unacceptable QPS deviations and a complete collapse of the system frequency. This is an unacceptable consequence. This frequency collapse occurs because the capacitor inrush current is large enough to evoke the sub transient reactance of the generator; hence the system response is commensurate with that described in section 3.3.1. The ensuing voltage drop cannot be corrected for by the AVR. This conclusion is supported by the results presented in Appendix L, which shows the full results of a test case run to simulate attempting to recharge the ESD in the manner suggested by Chaboki, et al., (2004). A plot of the resulting inrush current is shown in Figure 5-12, to enable comparison with further investigations in this section.

![Figure 5-12 GTA current following immediate recharge attempt](image)

Figure 5-12 shows the three phase GTA current on the y axis plotted against time on the x axis. Figure 5-12 shows that at the point of ESD recharge at 13.75 s an inrush current with a magnitude of approximately three times full load current occurs. This response is similar to the bolted three phase fault to earth response seen in Figure 4-17 obtained during the validation testing of the GTA in section 4.8.1. Hence, attempting to recharge the ESD in this manner has been shown to evoke a GTA response similar to a three phase fault.

Because the GTA load demand is a function of the capacitor voltage and charging current, when the capacitor voltage reduces to zero the load demand reduces to zero and an unbalance between the generation and load occurs. Aside from the EM railgun which represents the majority of the GTA load the only other connected load is the 2 MW EM railgun support load. This sudden loss of load triggers the GT load shed logic, described in section 4.4.1, which manages the GT fuel supply to prevent excessive over-speed and instability, thus balancing the system energy flow. As the ESD charging bridge is still demanding energy whilst the GT load shed logic is managing the fuel supply, energy is rapidly extracted from the GTA inertia and the GTA frequency collapses. This is demonstrated in Figure L-44 at Appendix L.
A candidate method of overcoming this problem is to oversize the ESD capacitor such that when a shot is fired it does not completely discharge. This means that when the ESD is reconnected to the GTA for another charge it is not empty, but contains an amount of residual charge. The advantages of this are fourfold:

1. The residual charge in the capacitor reduces the inrush current at the point of recharge.

2. The capacitor does not fully discharge following the shot the voltage does not reduce to zero thus the GTA retains load, viewing the discharge as a step load shed, rather than a complete load shed, as shown in Figure 5-13.

3. The supply side current harmonics are substantially reduced due to a smaller firing delay angle at discharge.

4. The recharging of the capacitor is achieved by a combined real and reactive current from the start of the recharging process. Furthermore, there is no requirement for the GTA to dissipate energy between shots, which could lead to the overheating of the brake resistor during continuous firing operations.

![Figure 5-13 ESD capacitor voltage and GTA real power during EM railgun continuous firing with an oversized ESD](image)

As shown in the upper plot of Figure 5-13, the GTA charges the ESD to its fully charged voltage of 11 kV within the ESD charge time. During this time the GTA attains power $x$, as shown in the lower plot. Once the ESD is fully charged the first shot is taken and 160 MJ is discharged from the ESD into the rails of the EM railgun. Following the discharge of the shot energy the ESD voltage drops, as shown in the upper plot. The magnitude of the voltage drop will depend on the total energy capacity of the capacitor assuming the depth of discharge remains the same. As the ESD does not fully discharge the voltage does not reduce to zero. Because of this the GTA retains load, viewing the discharge as a partial load shed rather than a
complete load shed, as shown in the bottom plot of Figure 5-13. The GTA then recharges the ESD with 160 MJ for the next shot and the process is repeated.

This method does however raise questions on the transient behaviour of the power system when conducting continuous firing operations in this manner and, on by how much to oversize the ESD. Furthermore, how the GTA load sheds taken following each EM railgun shot impact on QPS must be fully understood. As such, further research was conducted into this method, the results of which are presented in the following section.

### 5.4.2 Continuous firing investigation results

To assess the extent to which retaining residual charge in the ESD capacitor can limit the GTA load transients during continuous firing operations a series of increasing load sheds were taken and their corresponding maximum transient frequency and voltage deviations recorded, the results of which are shown in Figure 5-14 and Figure 5-15 respectively. The full results can be found at Appendix M. These plots were then used to predict the frequency and voltage deviations following the GTA load sheds occurring immediately after the shot is taken. The load steps were taken from an initial load of 39 MW and were increased from 1 MW to 13 MW in 1 MW increments. Beyond a 13 MW load shed model restrictions prevent further validated test data being obtained. The reason for this is that this particular GTA model is configured to maintain the GT speed below set limits, which correspond to a frequency deviation of approximately 4%. Beyond this the GTA load shed logic is triggered which manages the GTA fuel flow to prevent excessive GT over-speed which can result in instability. This is a safety feature built into the model which cannot be disabled, as confirmed in the letter from Rolls-Royce at Appendix E. Therefore, beyond a 13 MW load shed a true representation of the GTA behaviour is not obtainable and spurious results manifest. The implications and consequences of the GTA load shed logic with regards to this research are discussed further in section 6.2.2 in Chapter 6. The results presented in this section are the results sought as per the tests identified in section 5.2 and summarised in Table 5-1 for the case of continuous firing.

![Figure 5-14 GTA frequency deviation against load shed](image)
Results

Figure 5-14 shows the maximum frequency deviation on the y axis plotted against the corresponding GTA load shed on the x axis. Also shown on the plot are the NATO STANAG 1008 QPS frequency tolerance and transient tolerance limits. Figure 5-14 demonstrates that load sheds from 1 MW to 10 MW can be taken whilst remaining within the QPS tolerance limit and that load sheds between 11 MW and 13 MW can be taken whilst remaining within the QPS transient tolerance limit. As shown in Figure 5-14 the 13 MW load step corresponds to the transient tolerance limit of 4% which is the maximum allowable frequency deviation with this model.

![Figure 5-15 GTA voltage deviation against load shed](image)

Figure 5-15 shows the maximum voltage deviation on the y axis plotted against the corresponding GTA load shed on the x axis. Also shown on the plot is the NATO STANAG 1008 QPS voltage tolerance limit.

Figure 5-15 shows that load sheds from 1 MW to 13 MW can be taken whilst remaining within the QPS voltage tolerance limit, as defined by NATO STANAG 1008.

Further to anticipating the rise in frequency and voltage following a load shed, the total ESD size required to facilitate the corresponding load sheds was calculated and plotted in Figure 5-16. The corresponding calculations can be found at Appendix N. As shown from the characteristic of the curve plotted at Figure N-54 in Appendix N, limiting the load shed to less than 5 MW requires a prohibitively large ESD. Because of this 5 MW was selected as the lower load shed limit, as shown in Figure 5-16.
Figure 5-16 Total capacity of EM railgun ESD required against load shed

Figure 5-16 shows the total required energy capacity of the ESD on the y axis plotted against the corresponding load shed on the x axis. For example, to limit the GTA to a 9 MW load shed following each EM railgun shot a 400 MJ ESD must be employed to retain sufficient residual charge. This should in turn limit the voltage deviation to 1.75% and the frequency deviation to 2.50%.

5.4.3 Continuous firing case study

To investigate the previously discussed method of limiting the transient impact on QPS during continuous firing operations further two continuous firing case studies were conducted. One employed a 305 MJ ESD while the other employed a 440 MJ ESD. In both cases the aim was to fire six successive shots at a rate of one shot every 5 s. 5 s was selected at a start point based on the fact that the GTA can deliver 38 MW (when at 110% overload and when allowing for the 2 MW EM railgun support load) the GTA should be able to supply the required 160 MJ in approximately 5 s. It is acknowledged that the first shot will be fired after the initial ESD charge time, however subsequently a rate of fire commensurate with firing a shot every 5 s should be maintained. The purpose of conducting two case studies was to explore the extent to which the capacity of the ESD can limit the transient impact on QPS when conducting continuous firing operations by limiting the GTA load shed following each shot.

In the first case study a 305 MJ ESD was employed which corresponds to a GTA load shed of 12 MW, as derived from Figure 5-16. This was selected to demonstrate the impact of taking the largest permitted load shed with this GTA model thus minimising the required capacity of the ESD. A 1 MW margin was allowed between the absolute limit of 13 MW to remain within the envelope of validated modelling performance. Operating at the limit of the envelope can lead to triggering of the GTA load shed logic which can lead to spurious results. In the second case study a 440 MJ ESD was employed which corresponds to a load shed of 8 MW. This was selected to compare the impact of increasing the size of the ESD and reducing the GTA load shed on QPS, during continuous firing operations. The curves presented in Figure 5-14 and Figure 5-15 were then used to make predictions on the transient impact on QPS in both cases.
The corresponding capacitor characteristics were then calculated based on the data provided by Wolfe, et al., (2005). The GTA unload time was set to 4.50 s to maintain the both the frequency and voltage within tolerance, as per the single shot scenario case study. The ESD charge times were calculated based on the ratio of the size of the ESD to that employed in the single shot case study, where the charge time was 8.75 s. It was assumed that the relationship between the capacity of the ESD and the charge time is linear and that as the capacity of the ESD increases the charge time also increases. A summary of the system characteristics and the predicted impact on QPS is given in Table 5-5.

Table 5-5 System characteristics and predicted impact on QPS during continuous firing

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Case study 1</th>
<th>Case study 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>ESD capacity</td>
<td>305 MJ</td>
<td>440 MJ</td>
</tr>
<tr>
<td>ESD capacitance</td>
<td>5.04 F</td>
<td>7.27 F</td>
</tr>
<tr>
<td>Capacitor ESR</td>
<td>5.25 uΩ</td>
<td>3.63 uΩ</td>
</tr>
<tr>
<td>Initial ESD charge time</td>
<td>16.50 s</td>
<td>24 s</td>
</tr>
<tr>
<td>Predicted voltage deviation at ESD charge</td>
<td>18.50%</td>
<td>18.50%</td>
</tr>
<tr>
<td>Predicted frequency deviation at ESD charge</td>
<td>3%</td>
<td>3%</td>
</tr>
<tr>
<td>Predicted load shed at ESD recharge</td>
<td>12 MW</td>
<td>8 MW</td>
</tr>
<tr>
<td>Predicted voltage deviation at ESD recharge</td>
<td>2.50%</td>
<td>1.50%</td>
</tr>
<tr>
<td>Predicted frequency deviation at ESD recharge</td>
<td>3.60%</td>
<td>2.25%</td>
</tr>
<tr>
<td>Predicted voltage deviation at GTA unload</td>
<td>0.75%</td>
<td>0.75%</td>
</tr>
<tr>
<td>Predicted frequency deviation at GTA unload</td>
<td>3%</td>
<td>3%</td>
</tr>
</tbody>
</table>

To enable ease of comparison the power system characteristics have been separated into related plots as far as was practical. In each case the results of the 305 MJ ESD study are presented and discussed first. The equivalent results from the 440 MJ ESD study are then presented and the key differences highlighted. In both cases the ESD charging begins at 5 s which allows the simulation to be at steady state beforehand. Hence, for the case of the 305 MJ ESD study shots are taken at 21.50, 26.50, 31.50, 36.50, 41.50 and 46.50 s. The first shot is taken at 21.50 s which is the time taken to initially charge the 305 MJ ESD plus the 5 s required to allow the power system to be at steady state before the investigation begins. For the case of the 440 MJ study shots are taken at 29, 34, 39, 44, 49 and 54 s. The first shot is taken at 29 s which is the time taken to initially charge the 440 MJ ESD plus the 5 s required to allow the power system to be at steady state before the investigation begins. In both cases the GTA unloads following the final shot within 4.5 s. The results presented in this section are the results sought as per the tests identified in section 5.2 and summarised in Table 5-2 for the case of continuous firing.
Results

Figure 5-17 shows the GTA RMS current, RMS voltage and frequency on the y axis of the upper, middle and lower plot respectively, all plotted against time on the x axis for the 305 MJ ESD study.

Figure 5-17 shows that upon commencement of ESD charging there is an immediate demand for charging current which rises immediately from 250 A to 2.75 kA. Simultaneously, the GTA RMS voltage dips to approximately 9 kV (18.50%) which is within the maximum transient tolerance as defined by NATO STANAG 1008 and the GTA frequency dips to 59.50 Hz (0.83%) which is within tolerance as defined by NATO STANAG 1008. Throughout the ESD charging period the charging current remains constant at 2.75 kA, the GTA voltage recovers to 11 kV within 2 s and the GTA frequency remains at 59.50 Hz. At the point of ESD recharge a capacitor inrush current occurs. This can be seen in the upper plot of Figure 5-17 and is shown in greater detail in Figure 5-18.

Figure 5-17 also demonstrates that during continuous firing when employing a 305 MJ ESD the system is dynamically stable. This means that the transients resulting from each EM railgun shot and ESD recharge are repeatable and the system is not exhibiting any signs of degradation. This provides confidence that this rate of fire can be maintained. As shown in Figure 5-17 the system reaches dynamic stability by the fourth shot, at 36.50 s. As such, the voltage and frequency transients of the fourth shot were isolated and are shown in greater detail in Figure 5-19, from which more accurate voltage and frequency deviation values can be obtained.
Results

Figure 5-18 Three phase GTA current following ESD recharge with 305 MJ ESD

Figure 5-18 shows the three phase GTA current on the y axis plotted against time on the x axis. Figure 5-18 shows that at the point of ESD recharge at 21.50 s an inrush current with a magnitude of approximately 1.75 times full load current occurs.

When compared with that shown in Figure 5-12 the capacitor inrush current is much reduced, being approximately 1.75 times full load current as opposed to 3 times full load current. However, this inrush current still instigates a GTA voltage dip to 10.25 kV (-6.81%) which is within transient tolerance as defined by NATO STANAG 1008, as shown in Figure 5-19. This voltage deviation due to the inrush current is greater than the voltage deviation due to the GTA load shed, as predicted in Figure 5-15. Thus, based on the result presented in Figure 5-19 it can be concluded that the voltage deviation at the point of ESD recharge will be dominated by the voltage dip due to the inrush current and not by the voltage rise due to the GTA load shed, as predicted. With regards the GTA frequency Figure 5-19 demonstrates that at the point of ESD recharge the GTA frequency rises to 60.90 Hz (1.48%) which is within tolerance as defined by NATO STANAG 1008.
During the GTA unload the current decreases linearly to 250 A. The GTA voltage rises to 11.20 kV (1.80%) which is within tolerance and the GTA frequency increases to 61.50 Hz (2.50%) which is also within tolerance as defined by NATO STANAG 1008. As expected the GTA unload results are as per the single shot case study.

**Figure 5-20 GTA RMS current, RMS voltage and frequency during continuous firing operation – 440 MJ ESD**

Figure 5-20 shows the GTA RMS current, RMS voltage and frequency on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis for the 440 MJ ESD study.

Figure 5-20 also demonstrates that during continuous firing when employing a 440 MJ ESD the system is dynamically stable, as per the 305 MJ ESD study, thus providing confidence that this rate of fire could be maintained. As shown in Figure 5-20 the system reaches dynamic stability by the second shot at 34 s. To provide comparison with the 305 MJ ESD study the voltage and frequency transients of the fourth shot were isolated and are shown in greater detail in Figure 5-22, from which more accurate voltage and frequency deviation values can be obtained.

When comparing Figure 5-20 with Figure 5-17 a number of key differences were observed. Firstly, the inrush current at the point of ESD recharge is reduced. This is shown in greater detail in Figure 5-21, which demonstrates that the inrush current has been reduced from 1.75 times full load current to 1.38 times full load current.
Results

Figure 5-21 Three phase GTA current following ESD recharge with 440 MJ ESD

Figure 5-21 shows the three phase GTA current on the y axis plotted against time on the x axis. Figure 5-21 shows that at the point of ESD recharge at 29 s an inrush current with a magnitude of approximately 1.38 times full load current occurs.

Figure 5-22 GTA RMS voltage and frequency during continuous firing operation – 440 MJ ESD

Because of the reduced inrush current the voltage dip at the point of ESD recharge is reduced. As shown in Figure 5-22 at the point of recharge the voltage dips to 10.30 kV (-6.36%) which is within the transient tolerance as defined by NATO STANAG 1008. Lastly, the frequency deviation at the point of recharge is reduced to 60.75 Hz (1.23%) which is within tolerance as defined by NATO STANAG 1008.
Figure 5-23 GTA apparent, real and reactive power during continuous firing operation – 305 MJ ESD

Figure 5-23 shows the GTA apparent, real and reactive power on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis for 305 MJ ESD study.

Figure 5-23 shows that throughout ESD charging the GTA apparent power is between 50 and 60 MVA, while the GTA real power exhibits a linear increase during the charging period, increasing from supplying only the 2 MW EM railgun support load to 40 MW. Upon commencement of ESD charging the GTA reactive power increases to 60 MVAR before decreasing to 40 MVAR throughout the ESD charging period.

At the point of ESD recharge the inrush current can clearly be seen manifesting in each of the three power plots as short lived spikes of power. The GTA takes a 10 MW load shed following the shot which corresponds to the discharging of 160 MJ from the ESD. This is 2 MW less than the expected 12 MW. During the recharge period the GTA apparent power remains constant at 55 MVA, while the GTA real power ramps back to full power following the 10 MW load shed. The GTA reactive power decreases throughout each recharging period, from 50 MVAR to 40 MVAR. Following the sixth shot at 46.50 s the GTA unloads back to match the 2 MW EM railgun support load.

From the observations drawn from Figure 5-23 it can be concluded that during the early stages of the ESD charging the charging current is predominantly reactive and towards the end of the charging cycle is predominately real. This is also true for the ESD recharge cycles. These results are commensurate with
those presented in the single shot case study and the analytical analysis offered in section 3.4.1 and 3.4.2 thus supporting the verification of these results.

Figure 5-24 GTA apparent, real and reactive power during continuous firing operation – 440 MJ ESD

Figure 5-24 shows the GTA apparent, real and reactive power on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis for the 440 MJ ESD study. When comparing Figure 5-23 and Figure 5-24 the main difference is the magnitude of the GTA load shed following the shot, with the GTA taking an 8 MW load shed as opposed to the 10 MW load shed in the previous example. This result proves than increasing the capacity of the ESD can help to limit the GTA load shed following each shot.
Figure 5-25 shows the charging bridge firing delay angle, the GTA power factor and the main bus THD on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis for the 305 MJ ESD study.

Figure 5-25 shows that during the ESD charging period the charging bridge firing delay angle decreases linearly from 90 degrees to 25 degrees within the 16.50 s ESD charge time. Upon commencement of charging the GTA power factor decreases immediately from 0.75 lagging to 0.15 lagging then increases linearly back to 0.75 lagging throughout the charging cycle, as the firing delay angle decreases. The main bus THD peaks at 30% as charging commences, reducing to approximately 22.50% by the end of the charging cycle. This is outside the NATO STANAG 1008 QPS limit for THD, the implications and consequences of which will be described in Chapter 6. The impact of the firing delay angle on the GTA power factor and on the main bus THD at the point of ESD recharge can clearly be seen, with the GTA power factor decreasing then recovering as the firing angle resets to recharge the ESD. During the ESD recharge the GTA power factor is improved, decreasing to 0.50 lagging before recovering linearly to 0.75 lagging.

During the GTA unload the firing delay angle increases from 25 degrees to 90 degrees within the 4.50 s GTA unload time. The GTA power factor decreases to 0.35 lagging before returning to 0.75 lagging once
the GTA unload is complete. As the GTA unloads the main bus THD briefly increases to 25% before decreasing to 2.50% once the GTA unload is complete.

From the observations drawn from Figure 5-25 it can be concluded that the charging bridge firing delay angle impacts on both the power factor of the GTA and on the main bus THD during continuous firing. It is acknowledged that this characteristic is unique to the six-pulse thyristor rectifier. The observations drawn from Figure 5-25 are commensurate with those presented in the single shot case study and with the analytical analysis offered in section 3.4.1 and 3.4.2 thus supporting the verification of these results. Furthermore, it is demonstrated that these characteristics are repeatable when conducting continuous firing operations.

**Figure 5-26 Charging bridge firing delay angle, GTA power factor and main bus THD during continuous firing operation – 440 MJ ESD**

Figure 5-26 shows the charging bridge firing delay angle, the GTA power factor and the main bus THD on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis for the 440 MJ ESD study.

The key differences observed when comparing Figure 5-25 and Figure 5-26 are as follows. Firstly, the GTA power factor variation between shots is reduced, varying between 0.75 lagging and 0.60 lagging, as opposed to 0.75 lagging and 0.50 lagging. This is due to the fact that as the capacity of the ESD is larger the ESD capacitor voltage dip following the shot is reduced as a lesser proportion of the total stored energy is discharged, thus the variation in the firing delay angle is reduced. Also owing to the reduction in firing
Results

delay angle variation is the slight reduction of the main bus THD during the ESD recharge. It is acknowledged that the variations in GTA power factor and the main bus THD are minor when comparing the two case studies.

Figure 5-27 GTA energy supplied during continuous firing operation – 305 MJ ESD

Figure 5-27 shows the GTA energy supplied during continuous firing of the EM railgun on the y axis, plotted against time on the x axis. The energy flow was measured at the ESD charging bridge and does not include the energy supplied to the 2 MW EM railgun support load. Figure 5-27 proves that during the initial ESD charge stage the GTA delivers 305 MJ to the ESD. Figure 5-27 also proves that during each subsequent recharge the GTA delivers a further 160 MJ to the ESD, replacing what has been discharged for each shot. During the GTA unload stage it is demonstrated that the GTA generated 60 MJ of energy, which is dissipated in the ESD brake resistor.

Figure 5-28 GTA energy supplied during continuous firing operation – 440 MJ ESD

Figure 5-28 shows the GTA energy supplied during continuous firing of the EM railgun on the y axis, plotted against time on the x axis for the 440 MJ ESD study. The key difference when comparing Figure 5-28 and Figure 5-27 is the amount of energy initially delivered to the ESD, being 440 MJ as opposed to 305 MJ. As per the 305 MJ study, during each recharge 160 MJ of energy is delivered to the ESD. During the GTA unload stage 60 MJ of energy is generated.
5.4.4 Exploring the limits of continuous firing

To explore the limits of the rate of continuous fire achievable, for both the 305 MJ ESD case and the 440 MJ ESD case, the ESD recharge time between shots was reduced from 5 s in 0.25 s increments. In each case the GTA RMS voltage and frequency were plotted to determine if dynamic stability was maintained. The aim of this investigation was to determine at what point the system became unstable, hence defining the rate of continuous fire limit. The results presented in this section are the results sought as per the tests identified in section 5.2 and summarised in Table 5-1 for the case of continuous firing.

![GTA RMS voltage and frequency](image)

**Figure 5-29 Continuous firing - 305 MJ ESD – 4.75 s between shots**

Figure 5-29 shows the results of reducing the 305 MJ ESD recharge time between shots to 4.75 s. As shown, dynamic stability is achieved by the fourth shot at 35.75 s. As such, the voltage and frequency transients of the fourth shot were isolated and are shown in greater detail in Figure 5-30.

![GTA RMS voltage and frequency](image)

**Figure 5-30 GTA RMS voltage and frequency during continuous firing - 305 MJ ESD – 4.75 s between shots**
As shown in Figure 5-30, at the point of recharge the voltage dips to 10.25 kV (-6.81%) which is within the transient tolerance and the frequency dips to 58.80 Hz (-3.60%) which is also within the transient tolerance as defined by NATO STANAG 1008.

![GTA RMS voltage and frequency](image)

**Figure 5-31 Continuous firing - 440 MJ ESD – 4.75 s between shots**

Figure 5-31 shows the results of reducing the 440 MJ ESD recharge time between shots to 4.75 s. As shown, dynamic stability is achieved by the third shot at 38.50 s. As such, the voltage and frequency transients of the third shot were isolated and are shown in greater detail in Figure 5-32.

![GTA RMS voltage and frequency](image)

**Figure 5-32 GTA RMS voltage and frequency during continuous firing - 440 MJ ESD – 4.75 s between shots**

As shown in Figure 5-32, at the point of recharge the voltage dips to 10.35 kV (-5.90%) which is within the transient tolerance and the frequency dips to 59.30 Hz (-1.16%) which is within tolerance as defined by NATO STANAG 1008.
Figure 5-33 Continuous firing - 305 MJ ESD – 4.50 s between shots

Figure 5-33 shows the results of reducing the 305 MJ ESD recharge time between shots to 4.50 s. As shown, dynamic stability is not achieved and the GTA frequency exhibited continued degradation. From this result it can be concluded that the minimum time between shots when conducting continuous firing operations with a 305 MJ ESD must be 4.75 s to ensure that the system frequency remains stable.

Figure 5-34 Continuous firing - 440 MJ ESD – 4.50 s between shots

Figure 5-34 shows the results of reducing the 440 MJ ESD recharge time between shots to 4.50 s. As shown, dynamic stability is achieved by the fifth shot at 47 s. As such, the voltage and frequency transients of the fifth shot were isolated and are shown in greater detail in Figure 5-35.
Results

Figure 5-35 GTA RMS voltage and frequency during continuous firing - 440 MJ ESD – 4.50 s between shots

As shown in Figure 5-35, at the point of recharge the voltage dips to 10.35 kV (-5.90%) which is within the transient tolerance and the frequency dips to 57.25 Hz (-4.58%) which is within the maximum transient tolerance as defined by NATO STANAG 1008.

Figure 5-36 Continuous firing - 440 MJ ESD – 4.25 s between shots

Figure 5-36 shows the results of reducing the 440 MJ ESD recharge time between shots to 4.25 s. As shown, dynamic stability is not achieved and the GTA frequency exhibited continued degradation. This result demonstrates that the absolute minimum time between shots when conducting continuous firing operations with a 440 MJ ESD must be 4.50 s. Hence, this section has demonstrated that there is a minimum time between shots required to maintain dynamic system stability whilst conducting continuous firing operations. This minimum time was shown to be 4.75 s and 4.50 s when employing a 305 MJ ESD and a 440 MJ ESD respectively. This corresponds to a maximum firing rate of 12 and 13 shots per minute.
5.4.5 Continuous firing results summary

The results presented in this section have demonstrated that continuous EM railgun firing can be achieved whilst maintaining dynamic system stability. Furthermore, it has been demonstrated that an acceptable level of QPS can be maintained, as defined by NATO STANAG 1008. This was achieved by oversizing the ESD to retain residual charge following each shot. This in turn limits the GTA load shed following each shot which reduces the system transients and thus, the impact on QPS. A comparison between the predicted impacts on QPS following each EM railgun shot obtained from Figure 5-14, Figure 5-15 and Figure 5-16 and the actual impact on QPS obtained from the results of section 5.4.3 is offered in Table 5-6.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Predicted deviation</th>
<th>Actual deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>305 MJ ESD</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Voltage</td>
<td>2.50%</td>
<td>-6.81%</td>
</tr>
<tr>
<td>Frequency</td>
<td>3.60%</td>
<td>1.48%</td>
</tr>
<tr>
<td>GTA load shed</td>
<td>12 MW</td>
<td>10 MW</td>
</tr>
<tr>
<td>440 MJ ESD</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Voltage</td>
<td>1.50%</td>
<td>-6.63%</td>
</tr>
<tr>
<td>Frequency</td>
<td>2.25%</td>
<td>1.23%</td>
</tr>
<tr>
<td>GTA load shed</td>
<td>8 MW</td>
<td>8 MW</td>
</tr>
</tbody>
</table>

As demonstrated in Table 5-6 the accuracy of the predictions was poor. The accuracy of the voltage deviation prediction was unsatisfactory in both cases. This is because the voltage deviation was dominated by the voltage dip due to the ESD inrush current and not the over voltage due to the GTA load shed as was expected. However, improvement in the voltage deviation when increasing the capacity of the ESD was observed, thus successfully demonstrating the principle.

The actual GTA frequency deviation was less than the expected deviation in both cases. This is because the frequency deviations presented in Figure 5-14 were measured from a steady state frequency of 60 Hz, as opposed to the transient frequency shown in Figure 5-17 and Figure 5-20. This impacts on the overall deviation by introducing a transient offset. As this offset will be different depending on various ESD charging characteristics it cannot be reliably accounted for in the model. However, it can be concluded that Figure 5-14 can be used to predict the maximum possible frequency deviation following a specific load shed which makes for a useful reference when considering the transient impact of continuous firing on QPS. Improvement in the GTA frequency deviation was observed when increasing the capacity of the ESD, thus successfully demonstrating the principle.

In exploring the rate of fire limits when conducting continuous firing it was shown that to maintain dynamic stability the minimum ESD recharge time between shots must be 4.75 s and 4.50 s when employing a 305 MJ ESD and a 440 MJ ESD respectively, which corresponds to a maximum rate of fire of 12 or 13 shots per minute. To allow comparison between the impact of employing a 305 MJ ESD or a 440 MJ ESD on QPS the resulting voltage and frequency deviations pertaining to each of the previously discussed rates of fire are summarised in Table 5-7. The results presented in Table 5-7 are the results sought as per the tests identified in section 5.2 and summarised in Table 5-1.
### Table 5-7 Summary of EM railgun continuous firing on QPS

<table>
<thead>
<tr>
<th></th>
<th>Parameter</th>
<th>305 MJ ESD</th>
<th>440 MJ ESD</th>
<th>Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>ESD initial charge</strong></td>
<td>Voltage deviation</td>
<td>-18.50%</td>
<td>-15.90%</td>
<td>2.60%</td>
</tr>
<tr>
<td></td>
<td>Frequency deviation</td>
<td>0.60%</td>
<td>0.42%</td>
<td>0.18%</td>
</tr>
<tr>
<td></td>
<td>THD</td>
<td>30%</td>
<td>30%</td>
<td>0%</td>
</tr>
<tr>
<td><strong>GTA unload</strong></td>
<td>Voltage deviation</td>
<td>1.80%</td>
<td>1.80%</td>
<td>0%</td>
</tr>
<tr>
<td></td>
<td>Frequency deviation</td>
<td>2.50%</td>
<td>2.50%</td>
<td>0%</td>
</tr>
<tr>
<td></td>
<td>THD</td>
<td>25%</td>
<td>25%</td>
<td>0%</td>
</tr>
<tr>
<td><strong>5 s between shots</strong></td>
<td>Voltage deviation</td>
<td>-6.81%</td>
<td>-6.36%</td>
<td>0.45%</td>
</tr>
<tr>
<td></td>
<td>Frequency deviation</td>
<td>1.48%</td>
<td>1.23%</td>
<td>0.25%</td>
</tr>
<tr>
<td></td>
<td>THD</td>
<td>25%</td>
<td>22.5%</td>
<td>2.50%</td>
</tr>
<tr>
<td><strong>4.75 s between shots</strong></td>
<td>Voltage deviation</td>
<td>-6.81%</td>
<td>-5.90%</td>
<td>0.91%</td>
</tr>
<tr>
<td></td>
<td>Frequency deviation</td>
<td>3.60%</td>
<td>-1.16%</td>
<td>4.76%</td>
</tr>
<tr>
<td><strong>4.5 s between shots</strong></td>
<td>Voltage deviation</td>
<td>-</td>
<td>-5.90%</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>Frequency deviation</td>
<td>-</td>
<td>-4.58%</td>
<td>-</td>
</tr>
</tbody>
</table>

As shown in Table 5-7 the voltage and frequency deviations across the full range of continuous firing capability remained with the transient tolerance limits of ± 16% for voltage and ± 4% for frequency, with two exceptions. These exceptions were the voltage deviation during the initial ESD charge with the 305 MJ ESD and the frequency deviation at a rate of fire of 13 shots per minute with the 440 MJ ESD. In both cases the deviations were within the maximum transient limits. Furthermore, from the results presented in Table 5-7 it can be concluded that increasing the capacity of the ESD from 305 MJ to 440 MJ offers little improvement in terms of QPS and does not significantly improve the capability of the EM railgun, offering a maximum rate of fire of 13 shots per minute as opposed to 12. This is supported by the fact that the maximum rate of fire required is 12 shots per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015).

#### 5.5 Summary

The aim of this chapter was to present the results of simulation based research into the transient and harmonic impact of EM railgun operations on QPS for both single shot and continuous firing operations. The justification for investigating single shot operation was to determine the minimum time in which a single shot can be taken whilst maintaining an acceptable standard of QPS as defined by NATO STANAG 1008. The justification for investigating continuous firing was to ascertain the minimum time within which the GTA can continuously charge the ESD with 160 MJ in between the firing of shots, whilst the power system remains dynamically stable, thus defining the maximum rate of continuous firing achievable. The results of both of these investigations are useful to the naval design authority when assessing the capability of the EM railgun against the impact on QPS. The tests identified to assess the impact of EM railgun operations was described in section 5.2 and summarised in Table 5-1. Furthermore, single shot and continuous firing case studies were also identified and summarised in Table 5-2. The justification for the additional case studies was to allow further exploration of the system response by recording a wider range of results, which would not be practical to record over the range of tests summarised in Table 5-1.
When assessing single shot operations it was demonstrated that to remain within transient QPS limits as defined by NATO STANAG 1008 for both voltage and frequency transients the minimum time within which a complete single shot firing cycle can be completed is 12.77 s. This can be reduced to 10.50 s whilst remaining within the maximum transient QPS limits as defined by NATO STANAG 1008. These rates of fire were shown to be achieved by employing a 160 MJ capacitive ESD. It was also demonstrated that to maintain both voltage and frequency deviations within transient QPS limits as defined by NATO STANAG 1008 a minimum ESD charge time of 9.77 s must be allowed from the point of target acquisition. To maintain both voltage and frequency within maximum transient tolerance limits a minimum ESD charge time of 8.75 s must be allowed from the point of target acquisition.

When exploring continuous firing it was demonstrated that a maximum rate of fire of 12 shots per minute can be achieved whilst maintaining QPS within acceptable limits as defined by NATO STANAG 1008. This was achieved by increasing the capacity of the ESD from 160 MJ to 305 MJ, the premise of which was to retain residual charge following each shot. This residual charge limits the GTA load shed following each shot which reduces the system transients thus the impact on QPS. In exploring the limits of this concept it was shown that increasing the capacity of the ESD further from 305 MJ to 440 MJ offers little improvement in terms of QPS and does not significantly improve the capability of the EM railgun, offering a maximum rate of fire of 13 shots per minute as opposed to 12. This is not considered desirable, as the maximum rate of fire required is 12 shots per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015).

During both single shot and continuous firing operations the characteristics of the six-pulse thyristor rectifier used to control the rate of charge of the ESD was shown to introduce significant harmonic waveform distortion into the warship electric power system, with the main bus THD peaking at 30%. In all cases examined the main bus THD was outside of the QPS limit as defined by NATO STANAG 1008. This was also shown to impact heavily on the GTA power factor which decreased to 0.15 lagging during EM railgun firing operations. The implications and consequences of this will be discussed in Chapter 6.
Chapter 6 Discussion

6.1 Introduction

This chapter discusses the results presented in Chapter 5. Owing to the difference in analysis between the time-varying transient elements of QPS, being the voltage and frequency transients, and the repeatable periodic elements of QPS, being the harmonic waveform distortion, the impacts are discussed separately. In both cases the implications for the design of warship electric power systems are considered. Following this the consequences of the implications for the design of warship electric power systems comprising EM railguns are described. Mitigations to combat any unacceptable consequences identified are then offered, with the advantages and disadvantages argued in each case. Hence, this chapter is divided into the following four main parts all of which focus on warship electric power system QPS during EM railgun operations.

1. The first part discusses the time-varying transient impact on the RMS values of the warship electric power system voltage and frequency resulting from EM railgun operations.

2. The second part discusses harmonic waveform distortion, which repeats on a cyclic basis with the operation of the EM railgun, predominantly arising from non-linear elements in the circuit under investigation.

3. The third part presents potential system solutions to integrate an EM railgun with a warship electric power system, based on the discussion provided in the previous two sections. This part also presents the detailed design of a harmonic filter and the resulting performance of the warship electric power system with the 5th harmonic attenuated when conducting EM railgun operations.

4. Following discussion of the advantages and disadvantages of each of the potential system solutions, and drawing on the arguments offered throughout this chapter, the fourth part presents a justification for the most appropriate solution from an engineering science perspective to allow successful integration of an EM railgun with a warship electric power system.
6.2 Transient impact

As summarised in Table 2-3 in Chapter 2 the transient variation of electric warship power system RMS voltage and frequency is governed by QPS standards. As was discussed in section 2.2.3 and section 3.5, no specific rules and regulations for electric weapons are currently mandated. However as it is necessary to design the electrical system to operate within defined standards NATO STANAG 1008 was identified as a universal and acceptable starting point against which to formulate discussion (Kanellos, et al., 2006) (Lewis, 2006) (Kanellos, et al., 2007) (Tsekouras, et al., 2010) (Kanellos, et al., 2011) (Scuiller, 2012). Furthermore, it was concluded from Chapter 2 that assessing the impact of EM railguns on QPS against NATO STANAG 1008 is relevant for all NATO navies which may consider implementing EM railguns in the future. As such, the discussion in this section will be formulated with the NATO STANAG 1008 QPS standard in mind.

6.2.1 Results

The results presented in Chapter 5 were obtained from two distinct investigations. Section 5.3 presented the results of investigation into firing single shots, while section 5.4 presented the results of continuous firing. The justification for firing single shots is the requirement to fire warning shots or to target specific enemy positions. The justification for continuous firing is the requirement to provide naval fire support to forces shore, or to increase the chances of kill when prosecuting high speed missile and surface threats. As described in Chapter 5, the difference between single shot and continuous firing is the operational protocol under which the EM railgun is operated. As described in section 5.3.1 a single shot comprises a complete firing cycle, whereby the GTA begins in a state of equilibrium supplying only 2 MW to the EM railgun support load. 160 MJ is then transferred from the GTA to the ESD over a defined time period. The power system then returns to a state of equilibrium again over a defined time period, supplying only 2 MW to the EM railgun support load following the firing of a single shot, by unloading the GTA into a braking resistor. The braking resistor is required to dissipate any excess real power generated by the GTA during unload. As described in section 5.4.1, when operating under the continuous firing protocol the GTA begins in a state of equilibrium supplying only the 2 MW EM railgun support load then transfers 305 MJ to the ESD over a defined time period. In this case the GTA then transfers 160 MJ to the ESD following each shot over a defined time period, thus remaining in a transient state. Following the final shot in the continuous firing operation the GTA then returns to a state of equilibrium over a defined time period supplying only 2 MW to the EM railgun support load by unloading into a braking resistor.

**Single shot firing investigation results**

The single shot firing investigation, the results of which are presented in section 5.3.2, sought to increase the defined time during which the GTA supplied 160 MJ to the ESD from 7 s to 10 s at 0.25 s intervals. The characteristic of the curve plotted in Figure 5-3 suggests that attempting to charge the ESD in less than 7 s rapidly increases the frequency deviation below the NATO STANAG 1008 maximum transient tolerance limit, hence investigation below 7 s was not undertaken. In each case the RMS voltage and frequency transients were recorded. With reference to NATO STANAG 1008 the results of this
investigation demonstrated that to maintain QPS within the maximum transient tolerance and the transient tolerance for the case of the frequency, the minimum ESD charge time must be 8 and 8.38 s respectively. It was also found that when the ESD charge time is beyond 8.75 s the frequency deviation will be within tolerance, as defined by NATO STANAG 1008 QPS limits. It was also demonstrated that to maintain QPS within the maximum transient tolerance and the transient tolerance as defined by NATO STANAG 1008 for the case of the voltage, the minimum ESD charge time must be 8.25 and 9.77 s respectively. The investigation did not continue beyond 10 s as the characteristic of the curve plotted in Figure 5-4 suggested that maintaining the voltage deviation within the tolerance limit would require an unreasonably long charge time.

Following this the defined time during which the GTA unloads following a shot was increased from 2 s to 10 s at 0.5 s intervals. The results presented demonstrated that to remain within the maximum transient tolerance limit for the case of frequency the minimum GTA unload time must be 2.25 s. To remain within the transient tolerance limit the minimum unload time must be 3 s, while unloading in a minimum time of 4.25 s complies with the tolerance limit. The characteristic of the curve plotted in Figure 5-5 suggests that attempting to unload in less than 2 s would result in rapidly increasing the magnitude of the frequency deviation, hence investigation did not continue below an unload time of 2 s. It was also demonstrated that across the full range of unload times examined the voltage deviation remained within the tolerance limit. A summary of the results discussed here in the context of NATO STANAG 1008 is provided in Table 6-1.

### Table 6-1 Summary of minimum ESD charge and GTA unload times with corresponding NATO STANAG 1008 QPS voltage and frequency compliance for single shot

<table>
<thead>
<tr>
<th>Parameter</th>
<th>NATO STANAG 1008 QPS limit compliance</th>
<th>ESD charge time (s)</th>
<th>GTA unload time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage</td>
<td>Maximum transient</td>
<td>8.25</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>Transient</td>
<td>9.77</td>
<td>2</td>
</tr>
<tr>
<td>Frequency</td>
<td>Maximum transient</td>
<td>8</td>
<td>2.25</td>
</tr>
<tr>
<td></td>
<td>Transient</td>
<td>8.38</td>
<td>3.00</td>
</tr>
</tbody>
</table>

It is apparent from the results presented in Table 6-1 that the rate at which energy is transferred from the GTA to the ESD and the rate at which the GTA unloads following the firing of the shot impact on the power system voltage and frequency transients and on the waveform quality. With reference to the results summarised in Table 6-1 and in the context of NATO STANAG 1008, the following implications for the design of warship electric power systems comprising EM railguns from a QPS perspective can be drawn.

1. Figure 5-4 shows that transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8.25 s would result in a voltage dip below NATO STANAG 1008 QPS limits. This is an under-voltage situation.

2. Figure 5-3 shows that transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8 s would result in a frequency dip below NATO STANAG 1008 QPS limits. This is an under-frequency situation.
3. Figure 5-5 shows that unloading the GTA to return the power system to equilibrium following the firing of the shot in a time less than 2.25 s would result in a frequency rise above NATO STANAG 1008 QPS limits. This is an over-frequency situation.

As shown in Figure 5-6 the peak voltage transient did not exceed the NATO STANAG 1008 limits across the range of GTA unload times examined. Hence, the implications for the design of warship electric power systems comprising EM railguns from a QPS perspective manifest as under-frequency, under-voltage and over-frequency, all of which are dependent on the rate at which energy is transferred to the ESD and the rate at which the GTA unloads following the shot. In this case the level of QPS maintained is limited by the response of the GTA AVR and governor. As such, the consequences of under-frequency, under-voltage and over-frequency will be discussed in section 6.2.2.

**Continuous firing investigation results**

The continuous firing investigation, the results of which are presented in section 5.4.2 and 5.4.4 had two key aims:

1. Assess the extent to which retaining residual charge in the ESD capacitor can limit the GTA load transients during continuous firing operations.

2. Explore the limits of the rate of continuous fire achievable using the preferred solution.

This first investigation was in response to the cross examination of a previous method to facilitate continuous firing suggested by Chaboki, et al., (2004). Testing of this method found that aiming to continuously recharge the 160 MJ ESD employed in the single shot firing investigation resulted in capacitor inrush currents representative of three phase faults, as shown in Figure 5-12. The consequence of this was that the system frequency was found to collapse at the point of attempted recharge, as shown in Appendix L. Such scenarios occur because once a shot is taken and the ESD has been discharged to the EM railgun the capacitor is empty, or perhaps practically empty. As a result when the capacitor is reconnected to the GTA to be recharged an inrush current of three times rated current occurs which causes unacceptable QPS deviations and a complete collapse of the system frequency. A full description of this scenario is offered in section 5.4.1 and Appendix L.

As such, a novel method to facilitate continuous firing was suggested. This method was to oversize the capacitor capacity from the 160 MJ required for a single shot to 305 MJ. As such, when a shot is fired it does not completely discharge. This means that when the ESD is reconnected to the GTA for another charge it is not empty, but contains an amount of residual charge. The key advantages of this are that the residual charge in the capacitor reduces the inrush current at the point of recharge. Also, as the capacitor does not fully discharge following the shot the voltage does not reduce to zero thus the GTA retains load, viewing the discharge as a step load shed, rather than a complete load shed, as described in detail in section 5.4.1 and shown in Figure 5-13. This novel method of continuous EM railgun firing mitigates the unacceptable consequences demonstrated in Appendix L which result from the method suggested by Chaboki, et al.,
Hence, an investigation was undertaken to assess the extent to which retaining residual charge in the ESD capacitor can limit the GTA load transients during continuous firing operations. The results of this investigation were presented in section 5.4.2. To conduct the investigation GTA load sheds were taken from an initial load of 39 MW and were increased from 1 MW to 13 MW in 1 MW increments. In each case the resulting voltage and frequency transient was recorded. Beyond a 13 MW load shed the GTA load shed logic, described in section 4.4.1, is triggered and the load shed logic control system manages the GTA fuel flow to prevent excessive over-speed. However, the results presented in section 5.4.2 demonstrated that a 13 MW load shed can be taken whilst maintaining both the frequency and voltage deviations within the maximum transient QPS limits, as shown in Figure 5-14 and Figure 5-15 respectively. Following this, it was demonstrated by means of Figure 5-16 that a 12 MW load shed corresponds to a 305 MJ ESD. This capacity of ESD was selected with which to conduct further investigation into continuous firing. A 1 MW margin was allowed between the absolute limit of 13 MW to remain within the envelope of validated modelling performance and to avoid spurious triggering of the load shed logic.

To investigate the suggested method of limiting the transient impact on QPS during continuous firing operations an investigation was undertaken to simulate the firing of six successive shots at a rate of one shot every 5 s. 5 s was selected at a start point based on the fact that as the GTA can deliver 38 MW (when at 110% overload and when allowing for the 2 MW EM railgun support load), the GTA should be able to supply the required 160 MJ in approximately 5 s. The results of this investigation demonstrated that when firing at a continuous rate of one shot every 5 s the voltage and frequency transients were within acceptable QPS limits, as shown in Figure 5-17 and Figure 5-19. Furthermore the system remained dynamically stable throughout the continuous firing operation, as shown in Figure 5-17. This means that the transients resulting from each EM railgun shot and ESD recharge are repeatable and the system is not exhibiting any signs of degradation. This provides confidence that this rate of fire could be maintained. The results of the investigation presented in section 5.4.3 also demonstrated that increasing the capacity of the ESD beyond 305 MJ provides no further benefit from a QPS perspective. The results supporting this argument are summarised in Table 5-7. This investigation which addresses the first key aim of the continuous firing investigation yields the following implication for the design of warship electric power systems comprising EM railguns.

1. The capacity of the ESD required to facilitate continuous firing is 305 MJ. This capacity is driven by the magnitude of the permissible GTA load shed and not by NATO STANAG 1008 QPS limitations. The permissible GTA load shed is limited by the GTA load shed logic which manages the fuel flow following large load sheds to prevent excessive GT over-speed. The consequences of this implication will be discussed in section 6.2.2.

The second key aim of the continuous firing investigation was to explore the limits of the rate of continuous fire achievable using the preferred solution. To facilitate this, the rate at which the 160 MJ was delivered to the ESD between shots was reduced from 5 s in 0.25 s intervals. The results of this part of the investigation were presented in section 5.4.4. It was demonstrated that to maintain dynamic stability and the voltage and frequency transients within acceptable QPS limits as defined by NATO STANAG 1008 the minimum ESD recharge time between shots must be 4.75 s, when employing a 305 MJ ESD. This result is
shown in Figure 5-29. This corresponds to a maximum rate of fire of 12 shots per minute. As the electric power system achieved a state of dynamic stability during continuous firing it is argued that the number of successive shots could be increased beyond that demonstrated, provided that the rate of fire remains constant at one shot every 4.75 s. The operator may take the decision to only fire 2 shots, or another salvo, in succession as opposed to the 6 demonstrated in this research, however the same limitation exists, which is that the shots must be at a constant rate of one shot every 4.75 s or greater.

During this investigation it was also demonstrated that increasing the capacity of the ESD to 440 MJ allowed the time between shots to be reduced to 4.50 s whilst the power system remained dynamically stable and the voltage and frequency transients remained within acceptable QPS limits as defined by NATO STANAG 1008. This result was shown in Figure 5-34. Attempting to decrease the time between shots from the two limits pertaining to the two ESD capacities described here results in dynamic stability being lost and system frequency collapse occurring, as shown in Figure 5-33 and Figure 5-36. This part of the continuous firing investigation yields the following implication for the design of warship electric power systems comprising EM railguns.

2. The minimum time required between shots when conducting continuous firing operations with a 305 MJ ESD is 4.75 s. This minimum time is required to maintain the power system in a state of dynamic stability and to maintain the voltage and frequency transients within acceptable QPS limits as defined by NATO STANAG 1008. This time between shots can be reduced to 4.50 s if a 440 MJ ESD is employed. The consequences of this implication will be discussed in section 6.2.2. In both cases 160 MJ is transferred to the ESD between shots, the difference being that the 440 MJ ESD retains a greater residual charge between shots.

6.2.2 Consequences

As demonstrated in the previous section, the transient impact resulting from the operation of EM railguns has several implications for the design of warship electric power systems, the consequences of which are discussed further in this section. For the case of single shot firing implications were found to result from the rate at which energy was transferred to the ESD and from the rate at which the GTA unloads following the firing of the shot. Transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8.25 s was found to result in an under-voltage, while transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8 s would result in an under-frequency. Furthermore, unloading the GTA to return the power system to equilibrium following the firing of the shot in a time less than 2.25 s would result in an over-frequency. Under-voltage, under-frequency and over-frequency are considered voltage and frequency excursions outside the maximum transient limits defined by NATO STANAG 1008, as presented in Table 2-3. As described by Bayliss (1999) both under-voltage and under-frequency events are likely trigger protection relays which may result in the shedding of load or the tripping of the GTA. As the only load on the system is the EM railgun and associated support load one might be tempted to reconfigure the protection system whilst the EM railgun is in operation such that the voltage and frequency excursions are permitted. However, the resulting consequences for the power system should be understood thus are discussed further here.
Firstly, equipment and machines connected to the supply, which in the case of this research is the GTA and the EM railgun support load, will be subject to the voltage and frequency transients resulting from the EM railgun operation. It is likely that the machines and equipment comprising the EM railgun support load will be designed to operate within the constraints of NATO STANAG 1008. This is because they are LV loads for which the NATO STANAG 1008 QPS standard is mandated. Hence, departure from the expected voltage and frequency transients may result in abnormal operation of the EM railgun support loads, such as the reduced output or temporary overheating of pumps and fans driven by induction motors (Kundur, 1994a). Furthermore, vibratory stresses may occur in the GT turbine blades as the speed reduces which may result in fatigue and accelerated life consumption (Kundur, 1994a) (Bayliss, 1999). The hazards of over frequency are perhaps more obvious with the primary concern being excessive over-speed of the GTA, which may cause mechanical damage if the material strengths of the machine are exceeded or trip the protection relays on over-frequency. The combined consequences of under-voltage, under-frequency and over-frequency may cause mal operation of the EM railgun support load and reduce the design life of the GTA. Therefore, reconfiguring the protection system to allow the resulting excursions is not considered an acceptable solution. Hence, mitigations will be discussed in section 6.2.3.

The second implication resulting from the integration of the EM railgun with the warship electric power system was that the capacity of the ESD required to facilitate continuous firing was found to be driven by the magnitude of the permissible GTA load shed and not by the NATO STANAG 1008 QPS limitations. The resulting capacity of the required ESD was found to be 305 MJ. As the capacity of the ESD is not QPS driven it becomes apparent when considering Figure 5-14, Figure 5-15 and Figure 5-16 that if the load shed logic were adjusted to permit increased load sheds the capacity of the ESD may be reduced, whilst maintaining an acceptable standard of QPS during continuous firing operations as defined by NATO STANAG 1008. The advantage of this would be that the physical size of the ESD would reduce. However as described in section 4.4.1, the aim of the load shed logic feature is to manage the GTA fuel flow during loss of load to prevent excessive over-speed and cannot be removed. This is a safety feature built into the GTA to prevent damage as a result of excessive over-speed. Therefore, adjusting the load shed triggers would result in GTA over-speed, which may cause damage or accelerated life consumption. Hence, adjusting the GTA load shed triggers is not considered acceptable and mitigations will be discussed in section 6.2.3.

The final implication discovered from the results presented in Chapter 5 was that the minimum time between shots when conducting continuous firing operations with a 305 MJ ESD is 4.75 s. This minimum time between shots is required to maintain the power system in a state of dynamic stability and to maintain the voltage and frequency transients within acceptable QPS limits as defined by NATO STANAG 1008. The consequences of under-voltage, under-frequency and over-frequency during continuous firing operations are as per the single shot firing case. The consequence of this limitation imposed on the minimum time required to recharge the ESD between shots is that the continuous rate of fire is constrained to 12 shots per minute. However, as was concluded from section 2.2.3 in Chapter 2, the maximum rate of fire required is 10 – 12 shots per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015). Hence, this consequence is considered acceptable as the rate of fire achieved is commensurate with the rate of fire required.
Furthermore, the physical space penalty associated with increasing the capacity of the ESD to 440 MJ which would allow a rate of fire of 13 shots per minute can be negated as this exceeds the requirement.

**Summary**

Before mitigations relating to the consequences of the transient impact on the warship electric power system are discussed a summary of the discussion and arguments presented in this section is offered in Table 6-2.

**Table 6-2 Summary of implications and consequences resulting from the transient impact of EM railgun operations on warship electric power systems**

<table>
<thead>
<tr>
<th>Implication</th>
<th>Consequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8.25 s would result in an under-voltage situation.</td>
<td>Both the under-voltage and under-frequency events shown to occur by means of the results of this research are likely trigger protection relays which may result in the shedding of load or the tripping of the GTA (Bayliss, 1999).</td>
</tr>
<tr>
<td>Transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8 s would result in an under-frequency situation.</td>
<td>Under-speed can cause fatigue in GT turbine blades (Bayliss, 1999).</td>
</tr>
<tr>
<td>Unloading the GTA to return the power system to equilibrium following the firing of the shot in a time less than 2.25 s would result in an over-frequency situation.</td>
<td>Excessive over-speed of the GTA may cause mechanical damage if the material strengths of the machine are exceeded or may trigger the protection relays on over-frequency (Bayliss, 1999).</td>
</tr>
<tr>
<td>The capacity of the ESD required to facilitate continuous firing is 305 MJ. This capacity is driven by the magnitude of the permissible GTA load shed which is limited by the GTA load shed logic.</td>
<td>Adjusting the load shed triggers would result in increased GTA over-speed which may cause damage or accelerated life consumption.</td>
</tr>
<tr>
<td>The minimum time required between shots when conducting continuous firing operations with a 305 MJ ESD is 4.75 s. This minimum time is required to maintain the power system in a state of dynamic stability and to maintain the voltage and frequency transients within acceptable QPS limits as defined by NATO STANAG 1008. This time between shots can be reduced to 4.50 s if a 440 MJ ESD is employed although performance gains are marginal.</td>
<td>Limitations imposed on the minimum time required to recharge the ESD between shots limits the continuous rate of fire to 12 or 13 shots per minute.</td>
</tr>
</tbody>
</table>

**6.2.3 Mitigations**

As discussed in the previous section some of the implications of integrating EM railguns with warships have challenging consequences for the design of the electric power system thus suitable mitigations must be sought. As previously described, transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8.25 s was found to result in an under-voltage. Transferring the 160 MJ required for a single shot from the GTA to the ESD in a time less than 8 s would result in under-frequency. Furthermore, unloading the GTA to return the power system to equilibrium following the firing of the shot in a time less than 2.25 s would result in over-frequency. Under-voltage, under-frequency and over-voltage may trigger the protection relays or reduce the life expectancy of the GT by causing blade fatigue (Bayliss,
These consequences are unacceptable. It is therefore recommended that the minimum ESD charge time and GTA unload time be constrained to 8.25 s and 2.25 s respectively, such that unacceptable consequences be avoided. If higher firing rates are required then the continuous firing operations protocol must be adopted.

With regards to continuous firing operations it was found that the minimum time between shots required to maintain the power system in a state of dynamic stability, and to maintain the voltage and frequency transients within acceptable QPS limits as defined by NATO STANAG 1008 was 4.75 s. Reducing this minimum time between shots was found to induce under-frequency events which may trigger the protection relays or cause fatigue in the GT blades which may reduce life expectancy. These consequences are unacceptable. It was also found that reducing the ESD capacity below the 305 MJ suggested for continuous firing operations would require modifications to the GTA load shed logic triggers which would have unacceptable consequences for the GTA from a life expectancy and safety perspective. It is therefore suggested that the continuous rate of fire be limited to a maximum rate of 12 shots per minute and that an energy store of capacity 305 MJ be employed to maintain the life expectancy and safe operation of the GTA. The rate of fire to which it is recommended that continuous firing be constrained is commensurate with current requirements (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015).

The suggested constraints result to the impact of both single shot and continuous firing EM railgun operations on the transient elements of QPS being within acceptable limits as defined by NATO STANAG 1008. For the case of single shot firing no minimum required shot time has been defined in the literature, hence the minimum shot time suggested in this research is considered an acceptable starting point. With regards to continuous firing the required rate of fire of 10 – 12 rounds per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015) has been achieved whilst remaining within acceptable QPS limits as defined by NATO STANAG 1008. An advantage to the suggested constraints resulting to the voltage and frequency transients being within acceptable QPS limits is that machinery and equipment that may be exposed to the power system transients resulting from EM railgun operations, such as the GTA and the EM railgun support loads, can be designed to a common standard. This is useful to the naval design authority who may design such equipment in the knowledge that the voltage and frequency excursions resulting from the operation of the EM railgun to which other equipment may be exposed, should not exceed the limits of NATO STANAG 1008. It is highly recommended that these constraints be adhered to. Failing to adhere to the constraints detailed in this section forgoes any guarantee regarding the performance of the warship electric power system and any guarantee regarding the life expectancy of the GTA.

The suggested constraints which it has been argued should be placed on each firing protocol along with the corresponding capacity of the ESD required to maintain QPS within acceptable limits as defined by NATO STANAG 1008 and the life expectance and safe operation of the GTA are summarised in Table 6-3.
A notable observation from Table 6-3 is the definition of two distinct required ESD capacities relating to the two firing protocols. To enable continuous firing up to the required rate of 12 shots per minute (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015) a 305 MJ ESD is required. However, single shots can be fired within a minimum time of 10.50 s per complete firing cycle with a 160 MJ ESD. To facilitate both firing protocols with a single ESD the modular approach of the capacitor bank can be exploited such that the bank can be configured in accordance with the firing protocol required. For example, consider the modular ESD design shown in Figure 6-1. In this case 103 2.96 MJ capacitor modules, as described by Wolfe, et al., (2005), are connected in parallel to form the complete 305 MJ bank. If a single shot is required 54 of the 103 capacitors need only be connected, while if continuous firing is required the full bank is utilised. This approach also allows for operation under the single shot protocol with the capacitor bank in a degraded state, provided that 54 of the 103 capacitor banks remain serviceable. The capability of the EM railgun with regards to the rate of fire when operating with a degraded ESD will depend on the RC characteristics of the degraded bank which may not allow the minimum possible time between shots to be achieved.

![Figure 6-1 ESD modular design – 305 MJ total capacity](image)

### 6.3 Harmonic impact

This section discusses the repeatable periodic impact on the voltage and current waveforms resulting from EM railgun operations. As summarised in Table 2-3 in Chapter 2 the harmonic waveform distortion content of warship electric power system voltage and current waveforms is governed by QPS standards. However, as was discussed in section 2.2.3 and section 3.5, no specific rules and regulations for electric weapons are currently mandated. As it is necessary to design the electrical system to operate within defined standards with regards to waveform quality, NATO STANAG 1008 was identified as a universal and acceptable starting point against which to formulate research based discussion (Kanellos, et al., 2006) (Lewis, 2006) (Kanellos, et al., 2007) (Tsakouras, et al., 2010) (Kanellos, et al., 2011) (Scuiller, 2012). Hence, the discussion in this section will be formulated with the NATO STANAG 1008 QPS standard for waveform quality in mind.
6.3.1 Harmonic analysis

As discussed in section 3.4.1 the six-pulse thyristor rectifier draws non-linear current from the supply thus introduces harmonic waveforms into the electric power system, predominantly at 5, 7, 11 and 13 times the fundamental frequency the amplitude of which vary with the firing delay angle. This was described analytically in section 3.4.1 and observed throughout the time-domain simulation based investigations conducted in this research, the results of which are presented in Chapter 5. Harmonic waveforms are either positive, negative and zero sequence components. The relationship between these components is such that positive sequence components are equal in magnitude and have a 120° phase shift with sequence a,b,c; the negative sequence components are equal in magnitude and have a 120° phase shift with sequence a,c,b; and the zero sequence components are equal in magnitude and in phase and will only flow through earth connections. Zero sequence harmonics cannot flow in delta connections or in the absence of a ground connection (Wakileh 2001 d) (Bucknall, 2007 a). A summary of the harmonic phase sequence in a balanced three phase system is given in Table 6-4.

Table 6-4 Harmonic phase sequence in a balanced three phase system (Wakileh 2001 d)

<table>
<thead>
<tr>
<th>Harmonic</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>10</th>
<th>11</th>
<th>12</th>
<th>13</th>
<th>14</th>
<th>15</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sequence</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>-</td>
<td>0</td>
</tr>
</tbody>
</table>

For the case of this research, since a six-pulse rectifier is employed, the zero sequence harmonics (3, 6, 9, …) circulate in the secondary windings of the delta connected isolation transformer which presents as open circuit zero sequence impedance. Thus, zero sequence harmonics cannot propagate into the AC power supply system. Hence, the harmonics circulating in the AC power supply system will be of positive and negative sequence.

In the presence of harmonic waveform distortion Fourier analysis is used to represent the waveforms and will be employed when discussing the results presented in Chapter 5. Hence, the distorted voltage and current waveforms expanded into Fourier series can be expressed by Equation 41 and Equation 42.

\[ v(t) = \sum_{h=1}^{\infty} V_h \cos(h \cdot 2\pi f_n t + \theta_h) \]  \[ E41 \]

\[ i(t) = \sum_{h=1}^{\infty} I_h \cos(h \cdot 2\pi f_n t + \phi_h) \]  \[ E42 \]

Where:

- \( h \) is the harmonic order
- \( I_h \) is the \( h \)th harmonic peak current
- \( V_h \) is the \( h \)th harmonic peak voltage
- \( \phi_h \) is the \( h \)th harmonic current phase
- \( \theta_h \) is the \( h \)th harmonic voltage phase
- \( f_n \) is the nominal fundamental frequency
The RMS value of a function (F) when expressed in terms of Fourier series is given by Equation 43.

\[ F_{\text{RMS}}^2 = \frac{1}{T} \int_0^T f^2(t) \, dt = \frac{1}{2} \sum_{h=1}^{\infty} F_h^2 = \sum_{h=1}^{\infty} F_{h\text{RMS}}^2 \]  

\[ \text{[E43]} \]

Where T is the period \( \frac{1}{f_0} \).

Hence, in the presence of harmonic waveform distortion resulting from non-linear loads the RMS values of the voltage are current are expressed by Equation 44 and Equation 45.

\[ V_{\text{RMS}} = \sqrt{\sum_{h=1}^{\infty} V_{h\text{RMS}}^2} \]  

\[ I_{\text{RMS}} = \sqrt{\sum_{h=1}^{\infty} I_{h\text{RMS}}^2} \]  

\[ \text{[E44]} \]
\[ \text{[E45]} \]

Considering Equation 44 and Equation 45 it becomes apparent that in the presence of harmonic waveform distortion the RMS values of the voltage and current are higher than those observed under purely sinusoidal waveforms.

As explained in section 2.2.3, the total harmonic content of a waveform can considered as the sum of the harmonic components expressed as a percentage of the fundamental. This provides a measure of the harmonic content present in the electric power system which can then be considered against the limits set by NATO STANAG 1008. With reference to Equation 44 the total harmonic content of the voltage waveform can be expressed by Equation 46.

\[ THD_V = \frac{1}{V_{1\text{RMS}}} \sqrt{\sum_{h=2}^{\infty} V_h^2} \times 100\% \]  

\[ \text{[E46]} \]

Considering Equation 43 and Equation 44 it follows that in the presence of harmonic waveform distortion the real (P) and reactive (Q) power can be expressed by means of Equation 47 and Equation 48 respectively. This builds on Equation 12 from the theoretical analysis offered in section 3.4.1.

\[ P = \sum_{h=1}^{\infty} V_{h\text{RMS}} I_{h\text{RMS}} \cos(\theta_h - \phi_h) \]  

\[ Q = \sum_{h=1}^{\infty} V_{h\text{RMS}} I_{h\text{RMS}} \sin(\theta_h - \phi_h) \]  

\[ \text{[E47]} \]
\[ \text{[E48]} \]

In building on Equation 16 offered in section 3.4.1 it follows that in the presence of harmonic waveform distortion the apparent power (S) comprises real power (P) and reactive power (Q) at harmonic frequencies, as expressed in Equation 47 and Equation 48. Thus in the presence of harmonic waveform the power factor can be expressed by Equation 49.

\[ \text{power factor} = \frac{P}{S} = \frac{\sum_{h=1}^{\infty} V_{h\text{RMS}} I_{h\text{RMS}} \cos(\theta_h - \phi_h)}{\sum_{h=1}^{\infty} V_{h\text{RMS}} I_{h\text{RMS}}} \]  

\[ \text{[E49]} \]
Thus far, this analysis has demonstrated that non-linear loads which introduce harmonic waveform distortion into the warship electric power system will increase the RMS values of the voltage and current waveforms, impact on the GTA real and reactive power and impact on the system power factor. The consequences will be discussed in the context of this research, with reference to the results presented in Chapter 5, shortly.

6.3.2 Harmonic impact on current carrying conductors

In addition to the previously described impacts, non-linear loads such as the six-pulse thyristor rectifier used to charge the EM railgun ESD, impact on the AC current carrying conductors in the GTA, transformer and the power cables. When considering harmonics the impedance of AC current carrying conductors is given by Equation 50.

\[ Z(h) = R + jhX_L \]  

Where \( h \) is the harmonic order.

When considering Equation 50 is becomes apparent that the impact of harmonic waveform distortion on the real and reactive elements of the apparent power will have a greater impact on the reactive power. As explained in section 3.3.1 and 3.4.2 the reactive power demand is managed by the AVR, which increases the excitation field winding voltage \( (e_{fd}) \), thus increasing the field winding current \( (i_{fd}) \), to counter the voltage drop across the generators transient reactance under the increasing load current. Thus, increasing the reactive power demand on the GTA will have implications for the GTA excitation system. This exacerbated condition is due to the fact that the reactive element \( (X_L) \) of the impedance \( (Z) \) increases with harmonic number \( h \). This is because the reactive element is frequency dependant, as described by Equation 51.

\[ X_L = 2\pi f_h L \]  

The Skin effect

However, as the frequency increases with harmonic order the current flowing in electrical conductors migrates towards the surface of the conductors such that the conductor resistance increases (Wakileh 2001 c). This is referred to as the skin effect. Hence, in the presence of harmonic waveform distortion and taking into account the skin effect the synchronous alternator, transformer and cable impedance are expressed by Equation 52, Equation 53 and Equation 54 respectively (Wakileh 2001 c).

\[ \text{Synchronous alternator } Z(h) = \sqrt{R} \cdot R + jhX_L \]  

\[ \text{Transformer } Z(h) = h(R + jX_L) \]  

\[ \text{Cable } Z(h) = \sqrt{R} \cdot (R + jX_L) \]
The resulting harmonic impedance diagram for the positive and negative sequence harmonics is shown in Figure 6-2. The 2 MW EM railgun support load would likely comprise electric motors, transformers and cables. The resulting combined impedance is not shown in Figure 6-2 for the purposes of simplicity, however the harmonic impact on the 2 MW EM railgun support load is acknowledged.

**Figure 6-2 Candidate warship electric power system harmonic impedance diagram**

For example, consider the 5th and 7th harmonic impedances of the alternator employed in this research when the skin effect is neglected. The 5th harmonic is of negative sequence and the 7th harmonic is of positive sequence hence the negative and positive sequence reactance are employed respectively. The armature resistance and the positive and negative sequence reactance were obtained from the synchronous alternator data sheet in Appendix F. Neglecting the skin effect the 5th and 7th harmonic impedances are given by Equation 55 and Equation 56 respectively.

\[
Z(5) = 0.14 + j5(0.146) = 0.14 + j0.73 \quad [E55]
\]

\[
Z(7) = 0.14 + j7(0.12) = 0.14 + j0.84 \quad [E56]
\]

Consider now, the equivalent 5th and 7th harmonic impedances when the skin effect is taken into account, which are given by Equation 57 and Equation 58 respectively.

\[
Z(5) = \sqrt{5}(0.14) + j5(0.146) = 0.31 + j0.73 \quad [E57]
\]

\[
Z(7) = \sqrt{7}(0.14) + j7(0.12) = 0.37 + j0.84 \quad [E58]
\]

When comparing Equation 55 and Equation 56 with Equation 57 and Equation 58 respectively, it can be seen that the skin effect increases the AC resistance of the generator impedance at harmonic frequencies. As power flows in each of the harmonic circuits in the alternator it becomes apparent that in practical terms, the skin effect increases the real power flow. This increases the copper losses in the machine due increased \( I_h^2 R_h \) losses at each harmonic frequency. Hence, the total copper losses in an electrical machine in the
presence of harmonic waveform distortion becomes equal to Equation 59. The consequences of increased copper losses in the presence of harmonic waveform distortion will be discussed further in section 6.3.7.

\[
Total \ copper \ losses = \sum_{h=1}^{\infty} I_h^2 R_{ah} \quad [E59]
\]

### 6.3.3 Harmonic impact on machines comprising iron cores

Harmonic waveform distortion also has implications for machines comprising iron cores, which in the case of this research are the synchronous alternator and the transformer. These losses consist of hysteresis loss and eddy current loss. Hysteresis losses occur due to the magnetisation of the iron core and depend on the volume and quality of the magnetic material used. Eddy current losses are associated with the flow of eddy-currents either in the core of a transformer or in the core of a rotating machine (Wakileh, 2001 a). At fundamental frequency the hysteresis losses and eddy-current losses are given by Equation 60 and Equation 61.

\[
P_{h1} = \varepsilon f_n B_{m1}^{1.6} \quad [E60]
\]

\[
P_{e1} = \varepsilon f_n^2 B_{m1}^2 \quad [E61]
\]

Where:

- \(\varepsilon\) is a constant that depends on the quality and volume of the core material
- \(f_n\) is the nominal fundamental frequency
- \(B_{m1}\) is the maximum value of the rated flux density

However in the presence of harmonic waveform distortion Equation 60 and Equation 61 yield Equation 62 and Equation 63, for the total hysteresis and eddy current loss respectively. This is due to the non-linear currents flowing in the machine at harmonic frequencies \(I_h\).

\[
P_h = \sum_{h=1}^{\infty} P_{hh} = P_{h1} \sum_{h=1}^{\infty} h \left(\frac{I_h}{I_1}\right)^{1.6} \quad [E62]
\]

\[
P_e = \sum_{h=1}^{\infty} P_{eh} = P_{e1} \sum_{h=1}^{\infty} h^2 \left(\frac{I_h}{I_1}\right)^2 \quad [E63]
\]

Where \(h\) is the harmonic order

Thus, the total iron losses in an electrical machine in the presence of harmonic waveform distortion are equal to Equation 64.

\[
Total \ iron \ losses = P_h + P_e \quad [E64]
\]
Hence, in the presence of harmonic waveform distortion it becomes obvious that both the hysteresis losses and the eddy-current losses will increase in electrical machines above those expected at fundamental frequency under steady state conditions. These losses have been shown analytically to increase with harmonic frequency. In practice the eddy-current losses account for a greater proportion of the total machine losses than the hysteresis losses (Wakileh, 2001a). The consequences of increased iron losses in the presence of harmonic waveform distortion will be discussed further in section 6.3.7.

### 6.3.4 Harmonic impact on power cables

Operating in the presence of harmonic waveform distortion also has implications for the power cables beyond increased copper losses due to the skin effect. As discussed in section 4.7.3 the power cables modelled in this research are considered short transmission lines and are represented by the series impedance $R_c$ and $L_c$ only, negating the shunt capacitance to earth (Kundur, 1994j). However, in the presence of harmonic waveform distortion the capacitive reactance $X_c$ reduces, as described by Equation 65.

$$X_c = \frac{1}{2\pi f_x C_x} \quad [E65]$$

Because of the decrease in capacitive reactance with harmonic frequencies the shunt capacitance can represent a short to earth, as shown in Figure 6-3. This may give rise to harmonic resonance at higher harmonic frequencies through the resulting RLC circuit (Wakileh, 2001d). This may cause harmonic currents to circulate in the power cables which will increase the thermal stress on the power cable insulation and will accelerate the deterioration of the voltage dielectric.

![Figure 6-3 Candidate warship electric power system with cable shunt capacitance included](image)

However, as the shunt capacitance is negated in the power cable model used in this research this condition was not observable in the results presented in this thesis. Furthermore, the shunt capacitance was not available in the cable data provided by NexansAmerCable (2015) but could be calculated if required. This is acknowledged as a limitation of the model employed in this research.
6.3.5 Harmonic impact summary

A diagram summarising the impact of harmonic waveform distortion caused by the non-linear load characteristics of the six-pulse thyristor rectifier on the key components of the warship electric power system is offered in Figure 6-4. Components suffering increased copper losses are shown in red, while components suffering increased iron losses are enclosed in a blue box. As the EM railgun support load may comprise LV transformers and electric motors both copper and iron losses may manifest in the various component parts. However, for the case of this research the EM railgun support load is grouped as a single 2 MW, 0.75 power factor lagging load (Chaboki, et al., 2004).

![Figure 6-4 Impact of harmonic waveform distortion on the key components of the warship electric power system](image)

A summary of the implications resulting from operating in the presence of harmonic waveform distortion for each of the warship electric power system components is summarised in Table 6-5.

<table>
<thead>
<tr>
<th>Component</th>
<th>Implication</th>
</tr>
</thead>
<tbody>
<tr>
<td>GTA</td>
<td>Increased real power delivery</td>
</tr>
<tr>
<td></td>
<td>Increased reactive power delivery</td>
</tr>
<tr>
<td></td>
<td>Decreased power factor</td>
</tr>
<tr>
<td>Transformer</td>
<td>Increased iron losses</td>
</tr>
<tr>
<td></td>
<td>Increased copper losses</td>
</tr>
<tr>
<td>Power cables</td>
<td>Increased copper losses</td>
</tr>
<tr>
<td>EM railgun support load</td>
<td>Increased iron losses</td>
</tr>
<tr>
<td></td>
<td>Increased copper losses</td>
</tr>
</tbody>
</table>

6.3.6 Results

In building on, and in support of, the analytical discussion offered thus far in this section the results presented in Chapter 5 can now be analysed to assess the extent of the problems described in this section.
for the case of this research. Consider the harmonic waveform distortion observed from the results presented in Chapter 5 which was recorded in terms of THD. The THD was recorded during the single shot and continuous firing case studies presented in sections 5.3.3 and 5.4.3 respectively. The aim of both the single shot and continuous firing case studies was to examine the system response further when conducting EM railgun operations. For the case of the single shot case study the main bus THD was found to increase immediately from 2.50% to 30% on commencement of ESD charging, before decreasing linearly to 22.50% throughout the charging cycle. As the GTA unloads the main bus THD briefly increases to 25% before decreasing to 2.50% once the GTA unload is complete. This result is shown in Figure 5-9. For the case of the continuous firing case study the main bus THD was found to increase immediately from 2.50% to 30% on commencement of ESD charging reducing to approximately 22.50% by the end of the charging cycle. This characteristic was found to repeat for each ESD charging cycle. As the GTA unloads the main bus THD briefly increases to 25% before decreasing to 2.50%. This result is shown in Figure 5-25. Hence, the level of THD present during both single shot and continuous firing EM railgun operations is consistently outside the NATO STANAG QPS limit which is 5%. With regards to NATO STANAG 1008 this is considered unacceptable.

Due to the significant harmonic waveform distortion present a significant increase in copper and iron losses in the synchronous alternator and transformer are expected. A significant increase in the copper losses in the power cables is also expected. Furthermore, a significant impact on the EM railgun support load is expected. As such, the consequences of the increased copper and iron loss in the synchronous alternator and transformer and the increased copper losses in the power cables will be discussed in section 6.3.7, as will the consequences for the EM railgun support load.

The resulting implications of such THD levels for warship electric power systems comprising EM railguns can be further described by considering Figure 6-5 and Figure 6-6. Figure 6-5 and Figure 6-6 show the actual voltage and current waveforms in comparison to the fundamental waveform for the case of the single EM railgun shot presented in section 5.3.3. The voltage and current waveforms shown in Figure 6-5 and Figure 6-6 respectively relate to the RMS voltage and current waveforms shown in Figure 5-7 in section 5.3.3. At this point the power factor is 0.75 lagging, as shown in Figure 5-9. From Figure 6-5 and Figure 6-6 the periodic nature of harmonic waveform distortion can be observed.
Figure 6-5 Actual GTA stator phase voltage and GTA fundamental stator phase voltage

Figure 6-5 shows the actual GTA stator voltage during the single shot EM railgun operation in blue, against the fundamental GTA stator voltage in red. As shown in Figure 6-5 the peak value of the actual GTA stator voltage is greater than the fundamental voltage. Secondly, the rate of rise of the actual GTA stator voltage, or the \( \frac{dV}{dt} \), is also greater than the fundamental. Both of these factors have consequences for the warship electric power system which will be discussed in section 6.3.7.

Consider now Figure 6-6 which shows the actual GTA stator current during the single shot EM railgun operation in blue, against the fundamental GTA stator current in red. As shown in Figure 6-5 the peak value of the actual GTA stator current is greater than the fundamental current. Also, as per the case of the voltage waveform the rate of rise of the actual current waveform, or the \( \frac{di}{dt} \), is greater than the fundamental. Both of these factors have consequences for the warship electric power system which will be discussed in section 6.3.7.
As shown in Figure 6-5 and Figure 6-6 the peak values of the actual GTA stator voltage and stator current are higher than the peak value of the fundamental voltage and current. Because of this the GTA RMS stator voltage and stator current are higher than those observed with purely sinusoidal waveforms, as per Equation 44 and Equation 45 from the analytical analysis offered previously. Practically however, as shown in Figure 5-7 and Figure 5-17, the fundamental GTA RMS stator voltage remains nominally at the rated 11 kV, albeit with the notching shown in Figure 6-5 and transient excursions accepted. This is because the GTA AVR is able to regulate the fundamental GTA stator voltage as explained in section 3.3.1. However, as the GTA current is a function of the GTA stator voltage and the non-linear load demand characteristic of the six-pulse rectifier, as described in section 3.4.1, the increased GTA RMS stator current manifests. The resulting GTA RMS stator current for both the single shot and continuous firing operations, as shown in Figure 5-7 and Figure 5-17 respectively, are shown here in Figure 6-7 and Figure 6-8.

![Figure 6-7 GTA stator current during single shot firing](image)

Figure 6-7 shows the GTA RMS stator current during single shot firing. Figure 6-7 shows that the resulting GTA RMS stator current is 2.75 kA. This is greater than the rated GTA RMS stator current which is 2.36 kA. The GTA RMS rated stator current was calculated from the synchronous alternator data sheet provided at Appendix F. Point A on Figure 6-7 shows the current during the transition between the GTA charging the ESD and unloading into the brake resistor. In this case the transition is smooth. Point B on Figure 6-7 shows the current during the point at which the six-pulse rectifier is isolated from the main bus. This transition yields a slight step change in current as the load changes. The resulting voltage and frequency transients at this point which are shown in Figure 5-7 are considered acceptable hence no corrective action is required.

![Figure 6-8 GTA stator current during continuous firing](image)

Figure 6-8 GTA stator current during continuous firing
Figure 6-8 shows the GTA RMS stator current during continuous firing. Figure 6-8 shows that the resulting GTA RMS current is 2.75 kA. This is greater than the rated GTA RMS current which is 2.36 kA. Point A on Figure 6-8 shows the current during the transition between the GTA charging the ESD and unloading into the brake resistor. This transition yields a slight step change in current. Point B on Figure 6-8 shows the current during the point at which the six-pulse rectifier is isolated from the main bus. This transition also yields a slight step change in current. The resulting voltage and frequency transients at both point A and point B are shown in Figure 5-17 and are considered acceptable as defined by NATO STANAG 1008 hence no corrective action is required. The current spikes shown in Figure 6-8 are a result of the capacitor inrush current at the point of ESD recharge. The occurrence of these capacitor inrush currents was discussed in section 5.4.3 and the corresponding three phase current waveforms are shown in Figure 5-18. The voltage and frequency transients resulting from these inrush currents have been shown to be within NATO STANAG 1008 QPS limits from the results presented in section 5.4.3. The consequences of the increased GTA RMS stator current for the warship electric power system will be discussed in section 6.3.7.

In addition to the increased RMS current in the presence of harmonic waveform distortion the GTA real power and reactive power increase, as explained in Equation 47 and Equation 48. This was shown to be exacerbated for the case of the reactive power due to the fact that the reactive element (\(X_L\)) of the impedance (\(Z\)) increases with harmonic order (\(h\)). This is because the reactive element is frequency dependant, as described by Equation 51. As such the results presented in Chapter 5 will be analysed to investigate the extent and implications of the increase in reactive power with regards to warship electric power systems. Consider first, that the GTA model employed in this research comprises a 36 MW GT and a 45 MVA synchronous alternator with a rated power factor of 0.80 lagging. This rating is specified in the synchronous alternator datasheet which can be found in Appendix F. Hence, when delivering full load at rated power factor and considering the power triangle offered at Figure 3-10 in section 3.4.1 the reactive power delivered can be calculated as 27 MVAr.

As described in section 3.3.1 and 3.4.2 the reactive power demand is managed by the field excitation current (\(i_{fd}\)). Hence, the increased reactive power demand resulting from harmonic waveform distortion will also have implications for the generator AVR and field excitation current. To understand the implications of increasing the reactive power demand on the GTA the field current under rated conditions must be ascertained for the purposes of comparison. To ascertain the rated field current a test run of the GTA, which simulated supplying full power at rated power factor, was conducted. The results are presented in Figure 6-9. As shown in Figure 6-9 the generator field current was found to be 3.10 pu when supplying full load at rated power factor, which as shown in the upper plot of Figure 6-9 corresponds to 36 MW and 27 MVAr. This field current is rated against the no load rating. According to Figure F-12 of the synchronous alternator data sheet at Appendix F, at rated voltage and no load the field current is 303 A. This corresponds to 1 pu field current. This value was obtained from the data sheet as GT model restrictions prevent the GTA model being run with no load, as described in section 4.4.5. Thus, the 3.10 pu field current at rated load corresponds to an actual field current of 939.30 A.
Figure 6-9 GTA real and reactive power with corresponding field current

However, as shown in Figure 5-8 and Figure 5-23 from the results presented in Chapter 5, during single shot and continuous EM railgun operation the GTA reactive power demand is between 40 and 55 MVAr which is greater than the rated value. The implication of this is that the average excitation field current rises to 3.70 pu for both single shot and continuous firing operations across the ESD charging cycle, as shown in Figure 6-10 and Figure 6-11 respectively. This corresponds to an actual average field excitation current of 1121 A during EM railgun firing operations. This is greater than the rated field excitation current of 939 A.

Figure 6-10 GTA field excitation current during single shot firing
The points of transition labelled A and B in Figure 6-10 and Figure 6-11 correspond to the points of transition explained for Figure 6-7 and Figure 6-8. Point A corresponds to the transition between the GTA charging the ESD and unloading the GTA into the brake resistor. Point B corresponds to the point at which the six-pulse rectifier is isolated from the main bus. As shown in Figure 5-8 and Figure 5-23, which shows the reactive power delivery during single shot and continuous firing respectively, points A and B also correspond to step changes in the reactive power delivery, hence a step change in the excitation field current manifests. This is because the reactive power demand is managed by the field excitation current ($i_{fd}$), as explained in section 3.3.1 and 3.4.2. Hence, when the reactive power demand increases the field excitation current increases to counter the voltage drop across the generator internal resistance $r_a$ and reactance $jX_s$ at lagging power factors (Watson, 1981 a).

The characteristics of the field current plots shown in Figure 6-10 and Figure 6-11 do not exhibit any signs to suggest that the field current is limited. This provides confidence that this is the maximum field current required during EM railgun operations. Hence, an implication of increasing the reactive power demand on the GTA above the rated value is that the excitation field current will increase beyond rated the rated value. The consequences of this will be discussed in section 6.3.7.

In addition to yielding implications for the excitation field current increasing the reactive power demand on the GTA also has implications for the GTA power factor. This was described in Equation 48 and Equation 49 in the analytical analysis offered previously. For the case of single shot firing the results presented in Figure 5-9 in section 5.3.3 demonstrate that upon commencement of ESD charging the GTA power factor decreases immediately from 0.75 lagging to 0.15 lagging. The GTA power factor then increases linearly throughout the charging cycle to 0.75 lagging before decreasing linearly to 0.35 lagging during the GTA unload. The impact on the GTA power factor was seen to be a result of the composition of real and reactive current used to charge the ESD which depend on the thyristor firing delay angle ($\alpha$), as described in section 3.4.1 and 3.4.2. The results demonstrating this relationship are given in Figure 5-8 and Figure 5-9 and are commensurate with the theoretical circuit analysis offered in sections 3.4.1 and 3.4.2.

The results presented for case of continuous firing are commensurate with those presented for single shot firing. Figure 5-25 shows that on commencement of ESD charging the GTA power factor decreases immediately from 0.75 lagging to 0.15 lagging then increases linearly back to 0.75 lagging throughout the charging cycle, as the thyristor firing delay angle ($\alpha$) decreases. During the ESD recharge the GTA power factor increases linearly back to 0.75 lagging.
factor is improved, decreasing to 0.50 lagging before recovering linearly to 0.75 lagging throughout the recharging cycle. During the GTA unload the GTA power factor decreases linearly to 0.35 lagging before returning immediately to 0.75 lagging once the GTA unload is complete. The results presented in Figure 5-25 demonstrate that the charging bridge firing delay angle impacts on the GTA power factor during continuous firing which is commensurate with the analytical analysis offered in section 3.4.1 and 3.4.2 and Equation 47 to Equation 49 from the analytical analysis offered previously in this section. The consequences of the impact on the GTA power factor will be discussed in section 6.3.7.

Hence, this section has identified several implications for the warship electric power system which result from the harmonic waveform distortion present during EM railgun operations. These have been shown analytically and in the results of this research presented in Chapter 5. This harmonic waveform distortion arises due to the non-linear characteristics of the six-pulse thyristor rectifier employed to charge the ESD. The resulting implications for the design of warship electric power systems comprising EM railguns are summarised as follows.

1. In the presence of harmonic waveform distortion copper and iron losses will increase in electric power system components above those experienced at fundamental frequency. For the case of this research such components include the GTA, the transformer and the power cables.

2. The peak value of the actual GTA stator voltage is greater than the fundamental voltage.

3. The rate of rise of the actual GTA stator voltage, or the $dV/dt$, is greater than for the fundamental voltage.

4. The peak value of the actual GTA stator current is greater than the fundamental current.

5. The rate of rise of the actual stator current waveform, or the $di/dt$, is greater than for the fundamental current.

6. The GTA RMS stator current during EM railgun operations is 2.75 kA. This is greater than the rated GTA RMS stator current which is 2.36 kA.

7. The increased reactive power demand on the GTA resulting from EM railgun operations increases the excitation field current beyond rated the rated value.

8. The increased reactive power demand on the GTA resulting from EM railgun operations impacts on the GTA power factor.

The consequences of the implications for the design of warship electric power systems comprising EM railguns identified in this section will be discussed in section 6.3.7.
6.3.7 Consequences

As demonstrated in the previous section, the harmonic impact resulting from the operation of EM railguns has several implications for the design of warship electric power systems, the consequences of which will be discussed in this section. As previously described, the harmonic waveform distortion present during EM railgun operations is a result of the six-pulse thyristor rectifier employed to charge the ESD. The first implications identified, done so from an analytical perspective, were that in the presence of harmonic waveform distortion both the copper and iron losses will increase in electric power system components above those expected at fundamental frequency, as demonstrated analytically by Equation 52 to Equation 59. The increased copper losses are due to power flowing in each of the harmonic circuits which yield $I_h^2R_h$ losses at each harmonic frequency present. The consequence of this is the increased heating of the copper current carrying conductors in the electric power system components. The increased copper losses manifest in the GTA, transformer and power cables. The increased iron losses comprise the hysteresis losses and eddy current losses which were shown to increase in electrical machines above those expected at fundamental frequency, as demonstrated analytically by Equation 60 to Equation 64. These losses were shown analytically to increase with harmonic frequency. The consequences of the increased iron losses are the raising of the machine core temperature (Wakileh, 2001 a) and will apply to the GTA and the transformer. Hence, both the increased copper and iron losses result to the increased heating of the GTA, transformer and power cables. The increased heating in the equipment reduces efficiency due to increased heat losses and also increases thermal and insulation stresses which can reduce life expectancy (Wakileh, 2001 a) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007). Both of these consequences are unacceptable from a systems engineering perspective. Hence, mitigations will be discussed in section 6.3.8 with the aim of reducing the adverse impact on the GTA, transformer and power cable efficiency and life expectancy.

A further key implication of the harmonic waveform distortion identified is that the peak values of the GTA voltage and current are higher than the fundamental. This was demonstrated in Figure 6-5 and Figure 6-6. These higher than expected peak voltage and current values may interfere with protection systems by triggering over-current or over-voltage relays (Bayliss, 1999) (Prousalidis & Kourtesis 2013 b) and may cause the mal operation of control and communications systems which are sensitive to power quality (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007) if harmonic filters are not employed. For the case of this research the higher than expected peak voltages and currents may cause the mal operation of the EM railgun support load which may impact on the operability of the weapon which is considered unacceptable. As such, mitigations will be considered in section 6.3.8.

Also identified from Figure 6-5 and Figure 6-6 was the increase in the rate of rise of the distorted voltage waveforms ($dV/dt$) and the rate of rise of the distorted current waveforms ($di/dt$) when compared with the fundamental. The increased rate of rise of the voltage and current waveforms can inflict increased stress on the insulation of electrical machines and power cables which can lead to accelerated life consumption or alternator and transformer faults due to shorted turns (Hodge, 2002) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007). For the case of this research this may cause the accelerated life consumption of the GTA, transformer and power cables or may cause GTA and transformer faults which may interrupt the EM railgun firing operation. Hence, the non-sinusoidal nature of the voltage and current waveforms present
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during EM railgun may cause the mal operation of the EM railgun support load and may cause premature ageing of the GTA, transformer and power cables, none of which are considered acceptable from a systems engineering perspective. As such, mitigations will be discussed in section 6.3.8.

In addition to the previously discussed consequences the increased peak value of the distorted current waveforms also increases the GTA RMS stator current during EM railgun operations. As discussed in the previous section, the GTA RMS stator current was found to be is 2.75 kA during EM railgun operations which is greater than the rated GTA RMS current which is 2.36 kA. The impact of this increased RMS stator current is the increased heating of current carrying copper conductors. While increased heating will affect the alternator, transformer and power cables, an example of the impact on the alternator is offered here. The alternator has been chosen as an example due to the availability of information in the data sheet provided at Appendix F, with which an assessment of the impact can be made. The impact of the increased GTA RMS stator current in the presence of harmonic waveform distortion can be made by considering the rise in the alternator stator winding temperature due to the increased $I_{2}^2R_{s}$ losses.

To make a precise calculation of rise in temperature it is necessary to have stator winding characteristics. The temperature rise can be considered approximately proportional to the stator current squared (IEEE, 2011).

Hence, the proportional increase in the stator winding temperature can be approximately defined as:

\[
\text{Increase in stator winding temperature} = \left(\frac{I_{\text{RMS}}}{I_{1}}\right)^2 = \left(\frac{2.750}{2.361.89}\right)^2 = 1.36 \quad \text{[E66]}
\]

This calculation does not take into account the transfer of heat into the machine, however makes for a reasonable estimate when seeking to qualify the impact of the increased RMS stator current.

According to the alternator data sheet at Appendix F the stator winding insulation is rated for a maximum continuous temperature of 130°C. This corresponds to IEC class B insulation, which can withstand a maximum winding temperature of 130°C, however at this temperature accelerated life consumption will result (Rotor (UK) Limited, 2015). Hence, applying the approximated proportional rise in temperature to the rated value of the stator winding insulation yields:

\[
\text{Increase in stator winding temperature} = 1.36 \times 130^\circ C = 176.80^\circ C \quad \text{[E67]}
\]

Hence, the predicted increase in the temperature of the stator windings exceeds the current class of winding insulation which is only designed to withstand a maximum temperature of 130°C. Such an increase in the stator winding temperature over the maximum rated temperature may lead to breakdown of the stator winding insulation which can lead to premature ageing or failure and shorted turns (Arrillaga & Watson, 2003) (Bucknall, 2007a) (Evans & Hoevenaars, 2007). As such, to preserve the life expectancy of the GTA
and to ensure proper operation during EM railgun operations mitigating the consequences of the increased stator winding temperature will be discussed in section 6.3.8.

The previous section also identified implications resulting from the increased reactive power demand on the GTA during EM railgun operations. This increased reactive power demand in the presence of harmonic waveform distortion was demonstrated analytically in Equation 48. The first implication identified was that the increased reactive power demand on the GTA resulting from EM railgun operations increases the excitation field current beyond the rated value. The consequence of increasing the field current is that the alternator field winding is designed to continuously carry the rated field current, which for the case of this GTA was shown to be 939.30 A. The rated field current may be periodically exceeded in accordance with Figure 6-12, which shows the permitted time plotted against the corresponding field current, given as a percentage of the rated value. For example, the field winding can tolerate a field current of approximately 125% of rated, for 60 s, as shown by the red line in Figure 6-12. This is referred to as the permissible thermal overload. The curve plotted at Figure 6-12 is based on data points provided by Kundur (1994 b). While only 4 points were available at the reference, the resulting plot encompasses the area of interest arising from EM railgun operation in this research. Practically, the permissible thermal overload also depends upon ambient cooling.

![Figure 6-12 Synchronous alternator thermal overload characteristic](image)

The increase in field current from the rated continuous value of 3.10 pu to 3.70 pu represents an increase of 19.35%. Reading values from Figure 6-12 it can be concluded that the field current required during EM railgun operation, which is equal to 119.35% of rated, could be tolerated for approximately 80 s. Limiting the continuous EM railgun firing time to 80 s would severely constrain the capability of the weapon and is not commensurate with sustained combat operations. Hence, simply allowing this increased field current without taking corrective action to safeguard the alternator is not considered acceptable.

The consequence of this increased excitation field current beyond the permissible thermal overload time of 80 s is the increased heating of the field winding due to the increased \( I^2R_f \) losses. While a calculation of the precise rise in temperature is not possible without field winding characteristics, which are not available
in the data sheet given at Appendix F, the temperature rise can be considered approximately proportional to the rise in field current squared (IEEE, 2011).

Hence, the proportional increase in the field winding temperature can be approximately defined as:

\[
\text{Increase in field winding temperature} = \left( \frac{I_{h fd}}{I_{fd}} \right)^2 = \left( \frac{1121.10}{939.30} \right)^2 = 1.42 \quad [E68]
\]

According to the alternator data sheet at Appendix F the field winding insulation is rated for a maximum continuous temperature of 125°C. This corresponds to IEC class B insulation, which can withstand a maximum winding temperature of 130°C, however at this temperature accelerated life consumption will result (Rotor (UK) Limited, 2015). Hence, applying the approximated proportional rise in temperature to the rated value of the field winding insulation yields:

\[
\text{Increase in field winding temperature} = 1.42 \times 125°C = 177.50°C \quad [E69]
\]

This calculation does not take into account the transfer of heat into the rotor, however makes for a reasonable estimate when seeking to qualify the impact of the increased reactive power demand on the GTA.

Hence, the predicted increase in the temperature of the field winding exceeds the current class of winding insulation which is only designed to withstand a maximum temperature of 130°C. Such an increase in the field winding temperature over the maximum rated temperature may lead to breakdown of the field winding insulation (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007). Breakdown of the field winding insulation can lead to premature ageing, shorted turns, field grounds and thermal sensitivity, whereby the thermal expansion of the copper windings imparts forces on the steel forging through the rotor slots, that are also subjected to increased heating, which can cause the rotor to bow and vibrate (Zawoysky & Tornroos, 2001). As such, to preserve the life expectancy of the GTA and to ensure proper operation during EM railgun operations mitigating the consequences of the increased field winding temperature will be discussed in section 6.3.8.

The final implication identified in the previous section was the impact of the increased reactive power demand on the GTA resulting from EM railgun operations on the instantaneous GTA power factor as described analytically in section 6.3.1 by means of Equation 47 to Equation 49. This was also observed in Figure 5-9 and Figure 5-25 from the results presented in Chapter 5. In discussing the consequences of this implication the resulting GTA operation and impact on the power factor will be considered in the context of the generator capability diagram, as shown in Figure 6-13. The chart at Figure 6-13 has been replotted from that given in the generator data sheet, shown in Figure F-13 at Appendix F, for the purposes of clarity. The generator capability diagram plots MVAr on the y axis, against MWs on the x axis. Under excited MVAr refers to leading power factor conditions and over excited MVAr refers to lagging power factor conditions. While the real output power capability is limited by the prime mover capability as well as the power factor, the generators reactive power capability is limited by three factors being the field current
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limit, stator current limit and the end region heating limit (Kundur, 1994 g). The generator capability diagram serves to define these limits via three intersecting curves.

The first curve, shown between points A and B on Figure 6-13 represents the field current limit, with the limiting factor being the thermal limit of the field winding resulting from the $I_f^2R_f$ heat losses. The second curve, shown between points B and C represents the stator current limit, with the limiting factor being the thermal limit of the stator windings resulting from the $I_s^2R_s$ heat losses. The full extent of this curve is shown by the black dotted line in Figure 6-13. The third curve, shown between points C and D represents the thermal limit of the stator winding end regions and relates to under excited, leading power factor conditions. This occurs due to flux being produced by the stator current adding to flux produced by the field current, thus enhancing the flux in the end region which induces a heating effect (Kundur, 1994 g).

The resulting enclosed area, represented by the thick black line in Figure 6-13, represents the operating envelope of the generator with regards the previously discussed thermal limits. For the capability diagram shown in Figure 6-13 the cooler discharge air cannot exceed 40°C to ensure that the stator and field windings remain within their thermal limits (IEEE, 2011).

![Generator capability diagram](image)

**Figure 6-13 Generator capability diagram**

To help visualise the consequences of the increased reactive power demand on the GTA power factor for the case of a single EM railgun shot points E, F, G and H, which correspond to the operation of the EM
railgun, were plotted on Figure 6-14. The points plotted on Figure 6-14 are taken from the results presented in Figure 5-8 and Figure 5-9 in Chapter 5.

![Diagram showing generator capability diagram](image)

**Figure 6-14 Generator capability diagram showing ESD charging area of operation for single EM railgun shot firing**

Point E corresponds to the point of operation immediately before the ESD charging commences at which point the GTA is supplying only the EM railgun support load. Upon commencement of ESD charging the GTA power factor drops immediately to 0.15 lagging, shown as point F. During the ESD charging cycle the power factor recovers to 0.75 lagging, shown at point G. This recovery is approximately linear, as shown by Figure 5-9. Once the shot is fired the GTA unloads from point G to point H, which is at 0.3 power factor lagging. Once the firing cycle is complete the six-pulse rectifier disconnects from the supply and the GTA returns to point E.

When observing Figure 6-14 it becomes apparent that points F and G lie outside the thermal limits of both the field and stator windings and that the alternator is operating outside of its design curves. Hence, based on the current alternator design considered in this research operation in the F to G region shown in Figure 6-14 is not acceptable. As discussed previously such an increase in the stator and field winding temperature over the maximum rated temperature may lead to breakdown of the winding insulation which can lead to accelerated life consumption, failure, shorted turns, field grounds and thermal sensitivity (Zawoysky & Tornroos, 2001) (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007).
To help visualise the consequences of the increased reactive power demand on the GTA power factor for the case of continuous EM railgun firing points E, F, G, H and I, which correspond to the operation of the EM railgun, were plotted on Figure 6-15. The points plotted on Figure 6-15 are taken from the results presented in Figure 5-23 and Figure 5-25 in Chapter 5.

![Figure 6-15 Generator capability diagram showing ESD charging area of operation for continuous EM railgun firing](image)

Point E corresponds to the point of operation immediately before the ESD charging commences at which point the GTA is supplying only the EM railgun support load. Upon commencement of ESD charging the GTA power factor drops immediately to 0.15 lagging, shown as point F. During the ESD charging cycle the power factor recovers to 0.75 lagging, shown at point G. This recovery is approximately linear, as shown by Figure 5-25. During the recharge of the ESD, required during continuous firing operations, the power factor drops to 0.5 lagging, shown as point H, then recovers to point G during each recharge cycle. In each case the shots are fired at point G. Once the final shot has been fired the GTA unloads from point G to point I which is at 0.3 power factor lagging. Once the firing cycle is complete the six-pulse rectifier disconnects from the supply and the GTA returns to point E.

When observing Figure 6-15 it becomes apparent that points F, G and H lie outside the thermal limits of both the field and stator windings and that the alternator is operating outside of its design curves. Hence, based on the current alternator design operation in the F to G to H region shown in Figure 6-15 is not acceptable. As discussed previously such an increase in the stator and field winding temperature over the maximum rated temperature may lead to breakdown the winding insulation which can lead to accelerated
life consumption, failure, shorted turns, field grounds and thermal sensitivity (Zawoysky & Tornroos, 2001) (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007). As such, to preserve the life expectancy of the GTA and to ensure proper operation during single shot EM railgun operations mitigating the consequences of the increased field winding temperature will be discussed in section 6.3.8.

Summary
Before mitigations relating to the consequences of the harmonic impact on the warship electric power system are discussed a summary of the discussion and arguments presented in this section is offered in Table 6-6.

Table 6-6 Summary of implications and consequences resulting from the harmonic impact of EM railgun operations on warship electric power systems

<table>
<thead>
<tr>
<th>Implication</th>
<th>Consequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increased copper losses in the GTA, transformer and power cables.</td>
<td>Increased heating of the copper carrying conductors in the GTA, transformer and power cables which decreases efficiency and life expectancy (Arrillaga &amp; Watson, 2003) (Bucknall, 2007 a) (Evans &amp; Hoevenaars, 2007).</td>
</tr>
<tr>
<td>Increased iron losses in the GTA and transformer.</td>
<td>Increased heating of the GTA and transformer which decreases efficiency and life expectancy (Wakileh, 2001 a).</td>
</tr>
<tr>
<td>The peak values of the actual GTA voltage and current are greater than the fundamental.</td>
<td>Higher than expected peak voltage and current values may interference with protection systems by triggering over-current or over-voltage relays (Bayliss, 1999) (Prousalidis &amp; Kourtis 2013 b) and may cause the mal operation of control and communications systems which are sensitive to power quality (Arrillaga &amp; Watson, 2003) (Bucknall, 2007 a) (Evans &amp; Hoevenaars, 2007). This may also cause the mal operation of the EM railgun support load.</td>
</tr>
<tr>
<td>The rate of rise of the actual GTA voltage and current, or the $dV/dt$ and the $di/dt$, is greater than the fundamental.</td>
<td>The increased rate of rise of the voltage and current waveforms can inflict increased stress on the insulation of electrical machines which can lead to accelerated life consumption or failure (Hodge, 2002) (Bucknall, 2007 a).</td>
</tr>
<tr>
<td>The increased reactive power demand on the GTA resulting from EM railgun operation increases the excitation field current beyond the rated value.</td>
<td>Predicted increase in the temperature of the field winding which exceeds the current class of winding insulation. Such an increase in the field winding temperature over the maximum rated temperature may lead to breakdown the field winding insulation may lead to reduced life expectancy (Arrillaga &amp; Watson, 2003) (Bucknall, 2007 a) (Evans &amp; Hoevenaars, 2007).</td>
</tr>
<tr>
<td>The increased reactive power demand on the GTA resulting from EM railgun operations impacts on the instantaneous GTA power factor.</td>
<td>Alternator is operating outside of its design curves and exceeds the current and thermal limits of both the field and stator windings. Increase in the stator and field winding temperature over the maximum rated temperature may lead to breakdown the winding insulation which can lead to accelerated life consumption, failure, shorted turns, field grounds and thermal sensitivity (Zawoysky &amp; Tornroos, 2001) (Arrillaga &amp; Watson, 2003) (Bucknall, 2007 a) (Evans &amp; Hoevenaars, 2007).</td>
</tr>
</tbody>
</table>

When considering the summary of implications and consequences of the harmonic impact on the warship electric power system it becomes apparent that the current practice of warship electric power system design is not robust enough to withstand the rigours of EM railgun operations. This is due to the characteristics of the six-pulse thyristor rectifier used to control the rate of charge of the ESD having a significant harmonic
impact on the electric power system which may reduce the life expectancy of the GTA, transformer and power cables through increased copper losses, increased iron losses and increased insulation stress, in addition to causing the mal operation of the EM railgun support load and the protection system. This is perhaps most obviously demonstrated in Figure 6-14 and Figure 6-15 which clearly show the alternator operating outside of its design limits. While similar six-pulse thyristor rectifiers are currently employed on in service warships such as the RN Type 45 Destroyer and the Type 23 Frigate both of these warships employ six-pulse thyristor rectifiers to control the propulsion motors (Hodge & Mattick, 2008) (Gates, 2014). The difference in power requirement when compared with the application considered in this research is significant, with the rectifiers on the Type 23 Frigate driving a 3 MW motor and the rectifiers on the T45 driving a 20 MW motor (Hodge & Mattick, 2008). As demonstrated through the results presented in Chapter 5 the six-pulse rectifier employed to charge the EM railgun ESD must be capable of periodically handling the full power of the GTA, which is 40 MW. This is twice the power requirement of the six-pulse thyristor rectifier employed on the Type 45 destroyer. As such, the application under which the six-pulse rectifier is employed in this research, which is to facilitate the rapid transfer of high amounts of energy, is novel with regards to warships. Hence, if mitigations are not considered EM railgun operations may reduce the efficiency and life expectancy of the GTA, transformer and power cables. In addition to the impact on the GTA, transformer and power cables the harmonic waveform distortion may cause the mal operation of the EM railgun support load and the protection system. The fact that the EM railgun may have such an adverse impact on the warship electric power system is not acceptable from a systems engineering perspective, hence mitigations will be considered in section 6.3.8.

6.3.8 Mitigations

As discussed in section 6.3.7 the consequences summarised in Table 6-6 result to the fact that currently, the warship electric power system under consideration is not robust enough to withstand the rigours of EM railgun operations. It was also demonstrated in section 6.3.7 that reduced life expectancy, reduced efficiency and mal operation of loads sensitive to power quality may result. This is due to the magnitude of the harmonic waveform distortion introduced by the high power six-pulse thyristor rectifier required to charge the EM railgun ESD. Hence, mitigations can be considered either to increase the robustness of the warship electric power system such that the resulting harmonic waveform distortion does not adversely impact on the GTA, transformer or power cables or, to reduce the resulting harmonic waveform distortion to a level where the impact on the GTA, transformer and power cables is considered acceptable. In both cases, the impact on the EM railgun support load and the protection system should also be considered. The mitigations are considered in this section to be option 1 and option 2 respectively. This section offers a description of both options and discussion on the advantages and disadvantages in each case.

Before the mitigation options are discussed it is useful to consider the baseline system which is shown in Figure 6-16. An implication identified in the previous section, resulting from the harmonic waveform distortion present during EM railgun operations, was the possible mal operation of the EM railgun support load which may comprise control and communications systems which are sensitive to power quality (Arrillaga & Watson, 2003) (Bucknall, 2007 a) (Evans & Hoevenaars, 2007). However, as discussed in section 2.2.3 the NATO STANAG 1008 THD limit of 5% is mandated for any 440 V and 115 V supply.
As shown in Figure 6-16 the EM railgun support load is a 440 V load, hence a harmonic filter is required to reduce the THD at the EM railgun support load 440 V bus to 5%. Harmonic filters, discussed in section 2.2.2, have been previously installed in the LV electric power system of warships to meet the NATO STANAG 1008 5% THD limit (Gerrard, et al., 2007) (Butcher, et al., 2011) thus the same is assumed achievable here. As such, the impact of the harmonic waveform distortion on the EM railgun support load is not discussed further in this section. Instead, the focus is placed on the impact of harmonic waveform distortion on the GTA, transformer, power cables and protection system, which comprise the HV power system, resulting from EM railgun operations. The novel load considered in this research has not yet been practically experienced and HV QPS standards are not currently mandated, hence discussion in this section will focus on the HV power system.

![Figure 6-16 Baseline warship electric power system comprising an EM railgun](image)

**Option 1 - Increase electric power system robustness**

The aim of option 1 is to increase the robustness of the warship electric power system such that the harmonic waveform distortion resulting from the six-pulse rectifier does not adversely impact on the GTA, transformer, power cables or protection system. The components requiring an increase in robustness are highlighted in red in Figure 6-17. Firstly, increasing the robustness of the GTA will be considered. As shown in Figure 6-14 and Figure 6-15 the alternator is currently operating outside of its design limits for single shot and continuous firing operations respectively. This was due to the increased reactive power drawn in the presence of harmonic waveform distortion which was shown to exceed the field current limit curve A to B in Figure 6-14 and Figure 6-15. The increased field current was shown to increase the field winding temperature beyond the current class of field winding insulation, as discussed in section 6.3.7.
Furthermore the increased stator current was shown to exceed the full extent of the stator current limit curve B to C in Figure 6-14 and Figure 6-15. This increased stator current increased the stator winding temperature beyond the current class of stator winding insulation, as discussed in section 6.3.7.

**Figure 6-17 Warship electric power system comprising an EM railgun – Option 1 – Increase electric power system robustness**

It should be noted at this point in the argument that the real power delivered by the GT does not need to be increased. This is because the real power delivered by the GT considered in this research is sufficient to facilitate the required rates of fire whilst remaining within acceptable operating limits with regards to rotational speed and frequency. This was demonstrated throughout the results presented in Chapter 5 and discussed in section 6.2. As such, only the alternator rating need be increased to facilitate the delivery of the increased reactive power demand. This is a significant advantage, since the choice of large marine GT’s is realistically limited to the Rolls-Royce MT30 and the GE LM6000 which are both rated at 36 MW (Beno, et al., 2004).

Hence, to be robust enough to withstand the increased stator current, field current and reactive power drawn in the presence of harmonic waveform distortion the capability of the alternator must be increased such that the resulting EM railgun operation is within the alternator capability curves shown in Figure 6-14 and Figure 6-15. As shown in Figure 6-17, both the alternators would require an increase in robustness to ensure dual redundancy with regards the EM railgun operation. The concept of dual redundancy with regards EM railgun operation was discussed in section 3.4. While a complete re-design of the synchronous alternator is...
outside the scope of this thesis the required increase in capability can be estimated based on the results presented in Chapter 5 and by considering the capability diagrams shown in Figure 6-14 and Figure 6-15. To estimate the required increase in capability a design point was selected based on the alternator capability plots presented in Figure 6-14 and Figure 6-15. The design start point selected was point G which corresponds to the completion of ESD charging and is common to both Figure 6-14 and Figure 6-15. At this point the GTA is supplying 40 MW and approximately 40 MVAr when a small design margin is allowed, as shown in both in Figure 6-14 and Figure 6-15. If it is assumed that the rated power factor of the synchronous alternator is to remain at 0.8 lagging for a rated reactive power delivery of 40 MVAr the resulting synchronous alternator power triangle becomes that shown in Figure 6-18. It is assumed based on this rated operating point that the curve A to B and the extension of curve B to C would now encompass points F, G and H shown in Figure 6-14 and Figure 6-15. It is acknowledged that this is an assumption however it is thought reasonable to assume that the alternator capability curves A to B, B to C and C to D would increase linearly with point G. This means that ignoring harmonic waveform distortion the EM railgun operating points E, F, G, H and I would be within the capability curves of the alternator with the capability shown by Figure 6-18. Hence, the alternator capability shown in Figure 6-18 is that required to withstand the increased heating of the field and stator windings which result from the harmonic waveform distortion present during EM railgun operations.

![Figure 6-18 Synchronous alternator power triangle – Option 1](image)

However, increasing the capability of the alternator whilst retaining the original GT rating yields a significant disadvantage. The GT considered in this research has a real output power of 36 MW. The results presented in section 5.3 and section 5.4 have demonstrated this can be increased to 40 MW, which represents a 110% overload, whilst maintaining the transient impact within acceptable limits as defined by NATO STANAG 1008. However, if the alternator capability were to be increased to that demonstrated by the power triangle shown in Figure 6-18 the GT would only ever be able to drive the alternator at 75% of full load. This would be true for all operations, not only during the firing of the EM railgun. This is because in order to supply 40 MVAr at rated power factor, the alternator has a corresponding real power capability of 53 MW. As shown in the synchronous alternator data sheet at Appendix F the efficiency of the alternator decreases with decreasing load. Based on the current design at rated power factor the maximum efficiency is achieved at 36 MW and is 98.35%, as shown in Appendix F. The alternator efficiency is then shown to decrease as the load decreases and below 50% load there is a rapid decrease in the efficiency of the alternator. This is not acceptable from a systems engineering perspective. For the case of the warship electric power system considered in this research, which is presented at Figure 6-17, this disadvantage
would be exacerbated at low load conditions such as low speed transit, whereby the propulsion motors would not be running at full power, thus further decreasing the load on the alternator. Hence, increasing the capability of the alternator to withstand the impact of the six-pulse thyristor rectifier selected to charge the EM railgun ESD would decrease the alternator efficiency under all operational scenarios when compared with the current alternator.

With regards to the transformer the argument is perhaps simpler, as specific transformers exist to operate in the presence of harmonic waveform distortion. In the presence of harmonic waveform distortion it is common to specify a K rated transformer which are specifically designed to tolerate harmonics (Wakileh, 2001b) (Xirton Technologies, 2015). K rated transformers are designed to withstand the increased transformer heating which results from the increased copper and iron losses described in section 6.3.2 and section 6.3.3. K rated transformers provide a neutral wire twice the size of a phase conductor to account for circulating zero sequence harmonics, have windings designed with several smaller sized parallel conductors to reduce the impact of the skin effect and use insulated conductors which result in reduced losses (Wakileh, 2001b). The K rating, which is calculated by Equation 70, is a measure of the transformers ability to withstand the impact of the harmonic waveform distortion while operating within its designed temperature limits (Xirton Technologies, 2015).

\[
K = \sum_{h=1}^{H} \left( h \frac{I_h}{I_{n}} \right)^2
\]

Equation 70

Standard K factor ratings are 4, 9, 13, 20, 30, 40 and 50 (Wakileh, 2001b). For the case of an electric power system comprising non-linear loads a K factor of at least 20 is typically required (Xirton Technologies, 2015). A K rated transformer provides a good option to replace the transformer currently employed in this research as they are specifically designed to tolerate the increased copper and iron losses resulting from harmonic waveform distortion. The disadvantage is the increased cost over an equivalent standard transformer.

As discussed in section 6.3.2 the power cables suffer increased copper losses in the presence of harmonic waveform distortion due to the skin effect and the increased RMS current which increases the \(F R_{C}\) losses. Furthermore, the increased rate of rise of the voltage and current waveforms inflicts increased stress on the cable insulation which can lead to accelerated life consumption (Hodge, 2002) (Bucknall, 2007a). As described in Appendix G the power cables were originally specified to carry the rated GTA load current at the fundamental frequency which is 2.36 kA. However, as shown in Figure 6-7 and Figure 6-8 from section 6.3.6 in the presence of harmonic waveform distortion the cables are required to carry 2.75 kA. Thus, the number of cables should be increased such that the nominal current carrying capacity of each cable is not exceeded. Based on a current carrying capability of 528 A (NexansAmerCable, 2015) 6 cables would be required when operating in the presence of harmonic waveform distortion resulting from the six-pulse rectifier, as opposed to 5 at fundamental frequency. The advantage of this solution is that minimal extra cabling is required, with a single extra cable meeting the increased current requirement in the presence of harmonic waveform distortion. The disadvantages of this solution are that although the current requirement can be satisfied with a single extra cable, the consequences for power cables operating in the presence of...
harmonic waveform distortion still exist. These consequences are that the power cables would still suffer increased copper losses when compared with losses at the fundamental frequency due to the skin effect and the increased rate of rise of the voltage and current waveforms would continue to inflict increased stress on the cable insulation which can lead to accelerated life consumption (Hodge, 2002) (Bucknall, 2007 a).

A summary of the advantages and disadvantages of increasing the robustness of the warship electric power system GTA, transformer and power cables to withstand the harmonic waveform distortion resulting from EM railgun operations is offered in Table 6-7.

Table 6-7 Option 1 - Summary of advantages and disadvantages

<table>
<thead>
<tr>
<th>Solution</th>
<th>Advantages</th>
<th>Disadvantages</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increase alternator capability.</td>
<td>Alternator capable of EM railgun operations in terms of reactive power delivery.</td>
<td>Continuous part load operation would decrease alternator efficiency across all operational scenarios.</td>
</tr>
<tr>
<td>Employ K rated transformer.</td>
<td>Transformers available specifically to operate in the presence of harmonic waveform distortion.</td>
<td>Increased cost when compared with standard transformer.</td>
</tr>
<tr>
<td>Increase cable number from 5 to 6 between GTA, transformer and six-pulse thyristor rectifier.</td>
<td>Minimal extra cabling required.</td>
<td>Consequences of skin effect continue to manifest which increase copper losses.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Increased dV/dt and di/dt would continue to inflict increased stress on the cable insulation which can lead to accelerated life consumption (Hodge, 2002) (Bucknall, 2007 a).</td>
</tr>
</tbody>
</table>

In addition to the disadvantages pertaining to increasing the robustness of each of the specific electric power system components summarised in Table 6-7 there are also disadvantages to this option that will manifest at all electric power system components which are exposed to the harmonic waveform distortion resulting from the six-pulse rectifier. These components are highlighted in red in Figure 6-17. As this option makes no attempt to decrease the harmonic waveform distortion the higher than expected peak voltage and current values and the increased rate of rise of the voltage and current waveforms will continue to manifest. As discussed in section 6.3.7 the higher than expected peak voltages and currents may interfere with protection systems by triggering over-current or over-voltage relays (Bayliss, 1999) (Prousalidis & Kourtesis 2013 b). Furthermore, the increased rate of rise of the voltage and current waveforms will continue to inflict increased stress on the insulation of the GTA, transformer and power cables which can lead to accelerated life consumption when compared with operation at the fundamental frequency (Hodge, 2002) (Bucknall, 2007 a). Hence, while the GTA, transformer and power cables have been rated to withstand the rigours of EM railgun operation, accelerated life consumption would still occur when compared with operating the machines at the fundamental frequency.
Option 2 - Reduce harmonic waveform distortion

The aim of option 2 is to reduce the harmonic waveform distortion resulting from the six-pulse thyristor rectifier to a level where the impact on the GTA, transformer, power cables and protection system is considered acceptable. As discussed in section 2.2.2 harmonic filters are commonly employed to reduce the harmonic waveform distortion resulting from power electronic converters such as the six-pulse thyristor rectifier (Hodge, 2002) (Evans & Hoevenaars, 2007). The aim of harmonic filters is to attenuate harmonics through resonant circuits connected in parallel with the harmonic source, which in this case is the six-pulse rectifier. These resonant circuits are tuned at the targeted harmonic frequencies to provide a low impedance path for harmonic currents. A further advantage of harmonic filters is that the capacitive element of the filter can also provide reactive power compensation, by providing reactive power to the load, as opposed to it being drawn from the generator which adversely impacts on the power factor (Kundur, 1994 a) (Kundur, 1994 d). An apparent disadvantage of harmonic filters when compared with option 1 is that an additional piece of equipment is required which will incur additional cost and occupy additional physical space on-board the warship.

HV harmonic filters are currently installed on in-service warships employing six-pulse thyristors rectifiers such as the Type 45 Destroyer (Gerrard, et al., 2007). However, as the high power application under which the six-pulse thyristor rectifier is employed in this research is novel with regards to electric warships a bespoke filter will be designed here based on a set of requirements generated from the results of this research. As the design of harmonic filters is a relatively complex process section 6.4, which is dedicated to the design of the harmonic filter required for this application, has been added. Once designed, the performance of the power system under reduced harmonic waveform distortion will be investigated.

6.4 Harmonic filter design process

As described by Wakileh (2001 b) the design of harmonic filters comprises the following steps which will be followed in the filter design process presented in this section.

1. Ascertain harmonics to be targeted
2. Determine the reactive power requirement of the harmonic source
3. Define required filter bandwidth
4. Calculate filter component values

6.4.1 Ascertain harmonics to be targeted

To ascertain which harmonics to target for attenuation a Fast Fourier Transform was performed over a sample of the GTA stator voltage to obtain the corresponding harmonic spectrum. The sample of the GTA stator voltage waveform analysed relates to the RMS voltage waveform shown in Figure 5-7 in section 5.3.3 for the case of a single EM railgun shot. Two sample cases were analysed to ascertain the harmonic spectrum across the EM railgun firing cycle. The first sample was taken immediately after ESD charging commences. As this point the firing delay angle ($\alpha$) is closer to 90° and the harmonic waveform distortion
is at a maximum, as described in section 3.4.1. The second sample was taken immediately before ESD charging completes. As this point the firing delay angle (α) is closer to 0° and the harmonic waveform distortion is at a minimum, as described in section 3.4.1. The resulting waveform and harmonic spectrum are shown in Figure 6-19 and Figure 6-20. When considering Figure 6-19 and Figure 6-20 it should be noted that the fundamental, or harmonic order 1, has a magnitude of 100% however the y axis in the lower plot has been cropped to enable the magnitude of the lower order harmonics to be viewed in greater detail.

Figure 6-19 GTA stator voltage harmonic composition sample 1 – 0 Hz - 3000 Hz
As shown in Figure 6-19 and Figure 6-20, the harmonic spectrum is dominated by the 5th harmonic which has a magnitude of approximately 21% and 16% of the fundamental respectively. For the purposes of the filter design harmonics will be attenuated in order of magnitude until an acceptable system performance is achieved. With regards to what acceptable system performance entails, it should be noted that the aim of reducing the harmonic waveform distortion during EM railgun operations is to mitigate the consequences summarised in Table 6-6. Acceptable performance is not measured by the NATO STANAG 1008 QPS requirement of 5%, as this standard is not mandated for the HV system. As discussed earlier a harmonic filter is supplied to reduce the THD at the EM railgun support load to 5%, to meet the NATO STANAG 1008 THD limit which is mandated for the LV system.

6.4.2 Determine the reactive power requirement of the harmonic source

The reactive power requirement of the harmonic source, considered in this research to be the six-pulse thyristor rectifier, can be determined by considering Figure 6-21, which shows the generator capability diagram as presented in section 6.3.7. The diagram shown in Figure 6-21 comprises only the points plotted in Figure 6-14 and Figure 6-15 which are outside the alternator capability curves. These points are F, G and H and are based on the results presented in section 5.3.3 and section 5.4.3 from Chapter 5. Also shown in Figure 6-21 is the approximate difference in reactive power between the alternator field current limit curve A to B and the current operating points F, G and H.
Figure 6-21 Generator capability diagram showing harmonic filter MVAr requirement

Observing Figure 6-21 it can be seen that the largest discrepancy in terms of reactive power between any EM railgun operating point and the alternator field current limit curve A to B is 17.5 MVAr. This difference was observed between point F and the field current limit curve A to B. Hence, to ensure all the EM railgun operating points are within the alternator capability curves of the existing GTA, a harmonic filter able to compensate the GTA with 17.5 MVAr is required. This 17.5 MVAr of compensation is required to meet the reactive power demand of the harmonic source whilst maintaining the EM railgun operating points within the alternator capability curves.

6.4.3 Define required filter bandwidth

Thus far, it has been demonstrated by means of Figure 6-19 and Figure 6-20 that the dominant harmonic is the 5th and by means of Figure 6-21 that the reactive power compensation required is 17.5 MVAr. The required bandwidth of the harmonic filter will now be defined. Consider first that for a 60 Hz electric power system, under steady state conditions, the 5th harmonic frequency would exist at 300 Hz. However, for the case of the EM railgun operations considered in this research the harmonic waveform distortion is present during transient conditions. As shown in Figure 5-7 and Figure 5-17 from the results presented in Chapter 5 the GTA frequency varies within a tolerance band of ± 3%. It therefore follows, that the 5th harmonic may exist at 300 Hz ± 3% during EM railgun operations. Hence, to be properly effective the harmonic filter must be tuned to the 5th harmonic with a bandwidth of ± 3%. This corresponds to a filter bandwidth of 291 Hz to 309 Hz. The impact of the harmonic filter bandwidth required for EM railgun operations on the filter design will be discussed shortly.
6.4.4 Calculate filter component values

The requirements of the bespoke filter generated from the results of this research have now been defined. As such, the component values can now be calculated. As previously stated, harmonics were targeted in order of peak magnitude until an acceptable electric power system performance was achieved. Thus, as the 5th harmonic was identified as the dominant harmonic by means of Figure 6-19 and Figure 6-20 a 5th harmonic filter was designed first. The filter was designed as according to Wakileh (2001 b).

First, the capacitor reactance is defined by Equation 71.

\[
X_C = \frac{kV^2}{Q_c} = \frac{11^2}{17.5} = 6.91 \, \Omega 
\]  

[E71]

Where \( Q_c \) is the reactive power requirement (MVAr).

To target the \( h \)th harmonic the reactor size is given by Equation 72.

\[
X_L = \frac{X_C}{h^2} \times \frac{6.91}{25} = 0.28 \, \Omega 
\]  

[E72]

The resistance of the reactor is given by Equation 73 where the characteristic reactance \( X_n \) is given by Equation 74 and the \( Q \) factor is determined by the filter bandwidth, as given by Equation 75. The \( Q \) factor can be considered a measure of the filters response. A filter with a narrower bandwidth will have a higher \( Q \) factor thus a lower impedance, lower losses and a sharper response. A filter with a wider bandwidth will have a lower \( Q \) factor thus a higher impedance, increased losses and a less sharp response.

\[
R = \frac{X_n}{Q} = \frac{1.39}{16.6} = 0.084 \, \Omega 
\]  

[E73]

\[
X_n = \sqrt{X_L X_C} = \sqrt{(0.28)(6.91)} = 1.39 \, \Omega 
\]  

[E74]

\[
Q_{filter} = \frac{f_n}{f_{upper} - f_{lower}} = \frac{300}{291 - 309} = 16.6 
\]  

[E75]

The impedance at any frequency is then given by Equation 76.

\[
|Z_f(h)| = \sqrt{R^2 + \left( hX_L - \frac{X_C}{h} \right)^2} 
\]  

[E76]

And the admittance at any frequency is given by Equation 77.

\[
|Y(h)| = \left( \sqrt{R^2 + \left( hX_L - \frac{X_C}{h} \right)^2} \right)^{-1} 
\]  

[E77]
6.4.5 Harmonic filter design response and summary

The resulting frequency response has been plotted for both Equation 76 and Equation 77 and is shown in Figure 6-22 and Figure 6-23 respectively. Figure 6-22 and Figure 6-23 demonstrate that the filter exhibits low impedance and high admittance at the 5th harmonic frequency of 300 Hz and as such should successfully attenuate the 5th harmonic. The 5th harmonic filter design parameters are summarised in Table 6-8.

![Figure 6-22 5th harmonic filter impedance against frequency](image1)

![Figure 6-23 5th harmonic filter admittance against frequency](image2)
Table 6-8 5\textsuperscript{th} harmonic filter design parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$Q_C$</td>
<td>17.5 MVAr</td>
</tr>
<tr>
<td>$X_C$</td>
<td>6.91 $\Omega$</td>
</tr>
<tr>
<td>$X_L$</td>
<td>0.28 $\Omega$</td>
</tr>
<tr>
<td>$X_n$</td>
<td>1.39 $\Omega$</td>
</tr>
<tr>
<td>$Q$</td>
<td>16.6</td>
</tr>
<tr>
<td>$R$</td>
<td>84 m$\Omega$</td>
</tr>
</tbody>
</table>

The final three phase filter circuit diagram is shown in Figure 6-24. The delta connection prevents the flow of zero sequence harmonics.

A potential disadvantage of such a filter is that over time the capacitors can deteriorate (Wakileh, 2001 b). This can lead to arcing between the capacitor plates which can vaporise the dielectric medium causing the internal pressure to increase. This can lead to the capacitor exploding. This scenario recently occurred on the Queen Mary 2 cruise liner where a catastrophic failure of a capacitor within a harmonic filter caused the shut-down of all the propulsion motors and a vessel blackout (Marine Accident Investigation Branch, 2010).

6.5 Power system performance with the 5\textsuperscript{th} harmonic attenuated

The performance of the power system with the 5\textsuperscript{th} harmonic attenuated was then assessed by upgrading the performance tool described in Chapter 4 to include the 5\textsuperscript{th} harmonic filter designed in this section. The resulting performance tool system diagram is shown in Figure 6-25. The single shot and continuous EM railgun firing investigations described in section 5.3.3 and section 5.4.3 in Chapter 5 were then repeated with the performance tool shown in Figure 6-25. The filter parameters are as per Table 6-8. In each case, the harmonic filter was connected to the main bus at the charging sequence start time of time equal to 5 s. This allows the simulation initialisation transients to settle and for the system to be at steady state before the test case commences. At the same time the six-pulse rectifier is connected to the main bus to charge the ESD. The harmonic filter is disconnected from the main bus once the GTA unload is complete, at the same time as the six-pulse rectifier. This means that the harmonic filter and the six-pulse rectifier are connected and disconnected from the main bus simultaneously, the reason for this is described in section 6.5.1.
6.5.1 Harmonic filter connection to the main bus

The reason for connecting the harmonic filter and the six-pulse rectifier to and from the main bus simultaneously is to minimise the impact of the harmonic filter on the GTA at fundamental frequency. Before the six-pulse rectifier is connected to the main bus the GTA load is considered linear hence harmonic waveform distortion is minimal. However, while the 5th harmonic filter presents a low impedance to the 5th harmonic frequency the filter also presents an impedance to the electric power system at fundamental frequency, as shown in Figure 6-22. This may impact on the power system performance at fundamental frequency. As shown in Figure 6-22 and Table 6-8 at fundamental frequency the filter impedance is dominated by capacitive reactance. This would present the GTA with a leading power factor load. As shown in Figure 6-13 the alternator can tolerate a much lower leading power factor load when compared with a lagging power factor load. As described in section 6.3.7 exceeding the curve C to D shown in Figure 6-13, which relates to leading power factor conditions, can cause excessive heating of the stator winding end regions.

Under leading power factor conditions the GTA is importing MVARs, as opposed to exporting MVARs under lagging power factor conditions. This condition may occur when the harmonic filter is generating more reactive power than is absorbed by the harmonic source, considered in this case to be the six-pulse rectifier. As such, the field excitation current decreases to regulate the GTA voltage as opposed to increasing under lagging power factor conditions, as described in section 3.3.1. Further commentary on the occurrence and consequences of this condition will be provided with the analysis of the results presented in this section. It should be noted at this point in this argument that synchronous alternators are not designed to absorb, or import, reactive power per se, but have the capability to do so if the situation arises. This capability is limited by the curve C to D shown in Figure 6-13. The full results of this investigation are offered in Appendix O, while selected results are presented here to enable comparison with results presented in section 6.3.6 and section 6.3.7 and to support the development of the arguments offered in this section. The results offered in this section into the performance of the warship electric power system when supplying power for an EM railgun under reduced harmonic waveform distortion will be presented in the same order.
as those presented in section 6.3.6 and section 6.3.7. As such, the actual GTA phase voltage and current are presented first, followed by the GTA RMS current and the field excitation current. Finally, the operation of the GTA with the 5th harmonic attenuated will be considered in the context of the alternator capability diagram.

### 6.5.2 Harmonic filter response examination

Before the results are presented the response of the filter is examined to ensure that a functional and realisable filter has been designed. Firstly, to ascertain the resulting harmonic spectrum a Fast Fourier Transform was performed over a sample of the GTA stator voltage. As described previously, two sample cases were analysed to ascertain the harmonic spectrum across the EM railgun firing cycle. The first sample was taken immediately after ESD charging commences. As this point the firing delay angle (\( \alpha \)) is closer to 90° and the harmonic waveform distortion is at a maximum, as described in section 3.4.1. The second sample was taken immediately before ESD charging completes. As this point the firing delay angle (\( \alpha \)) is closer to 0° and the harmonic waveform distortion is at a minimum, as described in section 3.4.1.

The results are shown in Figure 6-26 and Figure 6-27. The sample of the GTA stator voltage waveform analysed relates to the RMS voltage waveform shown in Figure O-58 in Appendix O for the case of a single EM railgun shot with the 5th harmonic attenuated. When considering Figure 6-26 and Figure 6-27 it should be noted that the fundamental, or harmonic order 1, has a magnitude of 100% however the y axis of the lower plots was cropped to enable the magnitude of the lower order harmonics to be viewed in greater detail. Observing Figure 6-26 and Figure 6-27 it can be seen that the 5th harmonic is significantly reduced when compared with Figure 6-19 and Figure 6-20 and is now less than 2% in both sample cases. Other harmonics are also reduced. This is because a harmonic filter tuned to attenuate the 5th harmonic will also attenuate other harmonics to a lesser extent (Wakileh, 2001 b). Hence, it can be concluded that the 5th harmonic filter design presented in this section successfully attenuates the 5th harmonic. In addition to confirming the attenuation of the 5th harmonic the harmonic filter branch voltages and currents were also examined to ensure that no peak voltages or currents were unmanageable with regards component ratings. The results of this investigation are presented in Figure O-61 in Appendix O. No filter branch voltages or currents were found to be outside the ratings of the warship electric power system, thus the filter design is considered acceptable.
Figure 6-26 GTA stator voltage harmonic composition with 5th harmonic filter sample 1 –
0 Hz - 3000 Hz

Figure 6-27 GTA stator voltage harmonic composition with 5th harmonic filter sample 2 –
0 Hz - 3000 Hz
When comparing Figure 6-26 and Figure 6-27 with Figure 6-19 and Figure 6-20 it also becomes apparent that the THD is reduced from a maximum of 26.78% to 8.16% as ESD charging commences and from 22.50% to 6.68% as ESD charging completes. Figure O-60 and Figure O-64 in Appendix O also demonstrate that throughout both the single shot and continuous firing scenarios the THD was substantially reduced. For the case of the single shot case study Figure O-60 shows that the main bus THD was found to increase immediately from 2.50% to approximately 8% on commencement of ESD charging, before decreasing linearly to approximately 6.68% throughout the charging cycle. As the GTA unloads the main bus THD decreases to 2.50%. Figure O-64 shows that for the case of the continuous firing case study the main bus THD was found to increase immediately from 2.50% to 8% on commencement of ESD charging reducing to approximately 6.68% by the end of the charging cycle. This characteristic was found to repeat for each ESD charging cycle. As the GTA unloads the main bus THD decreases to 2.50%. It is acknowledged that the THD recorded in this investigation remains outside of the NATO STANAG QPS limit which is 5%. However, as discussed previously the aim of reducing the harmonic waveform distortion during EM railgun operations is to mitigate the consequences summarised in Table 6-6. For the case of EM railgun operations acceptable performance is not measured by the NATO STANAG 1008 QPS requirement of 5%, as this standard is not mandated at the HV bus. As such, the reduction in THD will now be discussed in the context of mitigating the consequences summarised in Table 6-6.

6.5.3 Impact of attenuating the 5th harmonic during EM railgun operations on power system performance

When considering the results presented in Appendix O the first point to note is that the transient impact on the power system which was discussed in section 6.2 is not significantly affected. Such is to say, that the voltage and frequency transients during both single shot and continuous firing operation with the 5th harmonic filter do not significantly differ from those discussed in section 6.2. For the case of continuous EM railgun firing this is demonstrated by Figure 6-28. Comparison between the voltage and frequency transients with the 5th harmonic present and attenuated for the case of single shot operation can also be made by comparing Figure O-58 in Appendix O with Figure 5-7 in Chapter 5.

Figure 6-28 shows the GTA RMS voltage and GTA frequency on the y axis of the upper and lower plot respectively, both plotted against time on the x axis. In each case the transients resulting from operating with the 5th harmonic present and with the 5th harmonic attenuated are given for the purposes of comparison. Points where differences in the transients occurred have been marked as points A, B, C and D, an explanation of which will be offered here. As shown in the lower plot of Figure 6-28 no significant difference was observed for the case of the GTA frequency transients thus the GTA frequency transients will not be discussed further. As shown by point A the voltage dip on commencement of ESD charging is less with the 5th harmonic attenuated than with the 5th harmonic present. This is because the peak magnitude of the initial demand for ESD charging current is less, as shown in Figure O-62 in Appendix O. The reason for the decreased initial demand for ESD charging current will be explained later in this section. As such, the voltage dips to 10.75 kV (-2.27%) which is within the QPS tolerance limit as defined by NATO STANAG 1008. Points B and C show that the GTA RMS voltage transients at the point of each shot and at the point of GTA unload are increased with the 5th harmonic attenuated. At point B the GTA RMS voltage
rises to 11.75 kV (6.81%) and at point C the GTA RMS voltage rises to 11.90 kV (8.18%), both of which are within the QPS tolerance limit as defined by NATO STANAG 1008. At point D the GTA RMS voltage dips to 10.50 kV (-4.45%) which is also within the QPS tolerance limit as defined by NATO STANAG 1008. The reason for the increased voltage transients is that with the 5th harmonic attenuated the AVR excitation field current is reduced. This is demonstrated in Figure 6-33 and Figure 6-34 later in this section.

The reason for this will also be discussed later in this section. The consequence of the reduced field current is that the AVR cannot recover the voltage as quickly following a transient. However, the resulting increased GTA RMS voltage transients do not exceed acceptable QPS limits as defined by NATO STANAG 1008. This means that the EM railgun firing constraints and ESD capacity required to maintain acceptable QPS, as summarised in Table 6-3, require no modifications to account for the introduction of the 5th harmonic filter.

Figure 6-28 GTA RMS voltage and frequency during continuous EM railgun firing with 5th harmonic present and attenuated

Secondly, as Figure 6-26 and Figure 6-27 demonstrate a significant reduction in harmonic waveform distortion during EM railgun operations with the 5th harmonic attenuated it can be concluded that the increased copper losses in the GTA, transformer and power cables due to the skin effect will decrease. Furthermore, it can be concluded that the increased iron losses in the GTA and transformer will also decrease. Decreasing the copper losses in the GTA, transformer and power cables and decreasing the iron losses in the GTA and the transformer will reduce the additional heating cause by harmonic waveform distortion thus increase the efficiency of electric power transmission. Furthermore, reducing the additional heating cause by harmonic waveform distortion will potentially increase the life expectancy of the power
system components, when compared to operating with the 5th harmonic present, by reducing the thermal stress on the insulation. This is a significant advantage of the 5th harmonic filter.

Consider now Figure 6-29 and Figure 6-30 which show the actual GTA voltage and current waveforms with the 5th harmonic attenuated. When compared with Figure 6-5 and Figure 6-6 the impact of harmonic waveform distortion is visibly reduced and the waveform more closely resembles a sinusoid. Figure 6-29 shows that the peak value of the actual GTA stator voltage is approximately equal to the fundamental and is much reduced when compared with Figure 6-5. Furthermore, the $dV/dt$ is also reduced when compared with Figure 6-5. The impact of reducing the peak values of the GTA stator voltage and reducing the $dV/dt$ will be discussed shortly. At this point the power factor is 0.9 lagging as shown in Figure O-60 in Appendix O.

![Figure 6-29](image)

**Figure 6-29** Actual GTA stator phase voltage and GTA fundamental stator phase voltage with 5th harmonic filter

Figure 6-30 shows that the peak value of the actual GTA stator current is approximately equal to the fundamental GTA stator current and very closely resembles a sinusoidal waveform. The $di/dt$ is also much reduced when compared with Figure 6-6.
The reduction in the peak values of the voltage and current waveforms demonstrated in Figure 6-29 and Figure 6-30 will mitigate the consequences associated with peak voltages and currents which are significantly greater than the fundamental, as observed in Figure 6-5 and Figure 6-6. Hence, the 5th harmonic filter can prevent mal operation of the protection system, control systems and communication systems which may be impacted by the harmonic waveform distortion resulting from EM railgun operations. This is a significant advantage of the 5th harmonic filter. Furthermore, the decrease in \( \frac{dV}{dt} \) and \( \frac{di}{dt} \) observed in Figure 6-29 and Figure 6-30 will reduce the stress on the insulation of the alternator, transformer and power cables which will increase the life expectancy of the components when compared to operating with the 5th harmonic present. This is a further advantage of the 5th harmonic filter.

As Figure 6-30 demonstrates a decrease in the peak value of the GTA stator current waveform it follows that the GTA RMS stator current should also decrease as a result of attenuating the 5th harmonic. This result is shown in Figure 6-31 and Figure 6-32 for the case of single shot and continuous firing respectively.

Figure 6-31 shows the GTA RMS stator current during single shot firing with the 5th harmonic attenuated. Figure 6-31 shows that the resulting GTA RMS stator current peaks at 2.25 kA. This is less than the rated GTA RMS stator current which is 2.36 kA. Hence, with the 5th harmonic attenuated the GTA stator current is reduced from 2.75 kA, as shown in Figure 6-7, to 2.25 kA during ESD charging. Point A on Figure 6-31 shows the current during the transition between the GTA charging the ESD and unloading into the brake
Discussion

resistor. This transition yields a slight step change in current. The resulting voltage and frequency transients corresponding to point A are shown in Figure O-58 in Appendix O. The resulting transients are considered acceptable hence no corrective action is required.

![Figure 6-32 GTA stator current during continuous firing with 5th harmonic filter](image)

**Figure 6-32 GTA stator current during continuous firing with 5th harmonic filter**

Figure 6-32 shows the GTA RMS stator current during continuous firing with the 5th harmonic attenuated. Figure 6-32 shows that the resulting GTA RMS stator current is nominally 2.25 kA. This is less than the rated GTA RMS stator current which is 2.36 kA. Hence, with the 5th harmonic attenuated the GTA stator current is reduced from 2.75 kA, as shown in Figure 6-8, to 2.25 kA during continuous EM railgun firing operations. The current spikes shown in Figure 6-32 are a result of the capacitor inrush current at the point of ESD recharge. The voltage and frequency transients resulting from these inrush currents have been shown to be within acceptable transient QPS limits as defined by NATO STANAG 1008 from the results presented in Figure O-62 in Appendix O. Point A on Figure 6-31 shows the current during the transition between the GTA charging the ESD and unloading into the brake resistor. This transition yields a slight step change in current. The resulting voltage and frequency transients corresponding to point A are shown in Figure O-62 in Appendix O. The resulting transients are within QPS limits as defined by NATO STANAG 1008 hence no corrective action is required.

The advantage of the reducing the GTA stator current during both single shot and continuous firing operations with the 5th harmonic attenuated is that the increased heating of the stator windings due to increased levels of harmonic waveform distortion, as demonstrated in section 6.3.7, will not manifest. It can therefore be concluded that with the 5th harmonic attenuated the stator winding temperature will remain within the limits of the current class of insulation. This means that accelerated life consumption due to the increased heating of the stator windings will not occur when the 5th harmonic is attenuated. This is a significant advantage of the 5th harmonic filter.

Point B in both Figure 6-31 and Figure 6-32 shows the GTA stator current rising towards the end of the GTA unload before the six-pulse thyristor rectifier is disconnected from the main bus. This is because as the GTA unloads into the brake resistor, which is controlled by the six-pulse thyristor rectifier, the reactive power drawn by the harmonic source decreases. Once the reactive power drawn by the six-pulse rectifier is less than 17.5 MVAr the 5th harmonic filter begins to dominate. This is because the harmonic filter is generating more reactive power than is being absorbed by the six-pulse rectifier. As such, the reactive power at the main bus begins to increase as the real power continues to decrease. This characteristic is shown in Figure O-59 in Appendix O. As described earlier the harmonic filter is predominantly capacitive.
thus a leading power factor scenario results at point B in Figure 6-31 and Figure 6-32. The consequences of this will be discussed in terms of the generator capability diagram later in this section.

This leading power factor condition manifests in the field excitation current during both single shot and continuous firing operations, as shown at point B in Figure 6-33 and Figure 6-34 respectively. The under excitation condition at point B shown in Figure 6-33 and Figure 6-34 occurs due to the fact that under leading power factor conditions the GTA is importing MVAr, as opposed to exporting MVAr under lagging power factor conditions. This is because the harmonic filter is generating more reactive power than is absorbed by the six-pulse thyristor rectifier. As such, the field excitation current decreases to regulate the GTA voltage as opposed to increasing under lagging power factor conditions, as described in section 3.3.1. This decrease in field excitation current is shown at point B in Figure 6-33 and Figure 6-34. As shown in Figure O-58 and Figure O-62 in Appendix O the GTA voltage remains within acceptable QPS limits as defined by NATO STANAG 1008 during this period of under excitation hence no corrective action is required. The consequences of under excitation will be discussed in terms of the generator capability diagram later in this section.

It also becomes apparent when observing Figure 6-33 and Figure 6-34 that the field excitation current is reduced for both single shot and continuous firing EM railgun operations when the 5th harmonic is attenuated. This is due to the decreased reactive power demand which as shown in Figure O-59 and Figure O-63 in Appendix O peaks at 35 MVAr as opposed to 55 MVAr as shown in Figure 5-8 and Figure 5-23 in Chapter 5 with the 5th harmonic present. The resulting field excitation current shown in Figure 6-33 and Figure 6-34 is approximately 3 pu. As described in section 6.3.6 the rated field excitation current is 3.1 pu. Hence, with the 5th harmonic attenuated the excitation field current does not exceed the rated value.
The advantage of the reduced GTA field current during both single shot and continuous firing operations with the 5th harmonic attenuated is that the increased heating of the field winding due to high levels of harmonic waveform distortion, as demonstrated in section 6.3.7, will not manifest. It can therefore be concluded that with the 5th harmonic attenuated the field winding temperature will remain within the limits of the current class of insulation. This means that accelerated life consumption due to the increased heating of the field winding will not occur when the 5th harmonic is attenuated. It is acknowledged that the rated value of the field current is periodically exceeded at the point of ESD recharge to regulate the GTA voltage dip resulting from the capacitor inrush current. These field current peaks are shown in Figure 6-34. However, the length of time for which the rated value of the field winding current is exceeded can be tolerated in accordance with the permissible thermal overload shown in Figure 6-12.

Thus far, this section has demonstrated that the attenuation of the 5th harmonic has several advantages. Firstly, it has been concluded that the reduction in THD from a maximum 26.76% to a maximum 8.16% will reduce copper losses in the GTA, transformer and power cables during EM railgun operations. Furthermore, it was concluded that the iron losses in the GTA and transformer will also decrease. Decreasing the copper losses in the GTA, transformer and power cables and decreasing the iron losses in the GTA and the transformer will increase the efficiency of electric power transmission and increase the life expectancy of the power system components when compared to operating with the 5th harmonic present. Secondly, it has been demonstrated that with the 5th harmonic attenuated the $dV/dt$ and $di/dt$ of the voltage and current waveforms is substantially reduced. This will reduce the stress on the insulation of the alternator, transformer and power cables which will increase the life expectancy of the power system components, when compared to operating with the 5th harmonic present. Thirdly, attenuation of the 5th harmonic was found to reduce the peak magnitude of the voltage and current waveforms which may prevent mal operation of the protection system during EM railgun operations. Lastly, the GTA RMS stator current and field excitation current have been shown to remain within their rated limits with the 5th harmonic attenuated. It can therefore be concluded that with the 5th harmonic attenuated the stator winding temperature and the field winding temperature will remain within the limits of the current class of insulation in both cases. Therefore, with the 5th harmonic attenuated accelerated life consumption is not expected.

6.5.4 Attenuating the 5th harmonic in the context of the generator capability diagram

To visualise the impact of attenuating the 5th harmonic the resulting single shot EM railgun operation points E, F, G and H were plotted on the generator capability diagram shown in Figure 6-35. The points plotted on Figure 6-35 were taken from the results presented in Figure O-59 in Appendix O. Point E corresponds to the point of operation immediately before the ESD charging commences at which point the GTA is supplying only the EM railgun support load. Upon commencement of ESD charging the GTA power factor drops immediately to 0.15 lagging, shown as point F. During the ESD charging cycle the power factor recovers to 0.90 lagging, shown as point G. Once the shot is fired the GTA unloads from point G to point H, which is at 0.40 power factor leading. Once the firing cycle is complete the six-pulse rectifier disconnects from the main bus and the GTA returns to point E. When observing Figure 6-35 it becomes apparent that with the 5th harmonic attenuated all the GTA operating points lie within the thermal limits of both the field and stator windings and that the alternator is operating within its design capability curves. Hence, with the
5th harmonic attenuated no accelerated life consumption of the GTA is expected during single shot EM railgun operations.

To visualise the impact of attenuating the 5th harmonic the resulting continuous firing EM railgun operation points E, F, G, H and I were plotted on the alternator capability diagram shown in Figure 6-36. The points plotted on Figure 6-36 were taken from the results presented in Figure O-63 in Appendix O. Point E corresponds to the point of operation immediately before the ESD charging commences at which point the GTA is supplying only the EM railgun support load. Upon commencement of ESD charging the GTA power factor drops immediately to 0.15 lagging, shown as point F. During the ESD charging cycle the power factor recovers to 0.90 lagging, shown as point G. During the recharge of the ESD, required during continuous firing operations, the power factor drops to 0.70 lagging, shown as point H, then recovers to point G during each recharge cycle. In each case the shots are fired at point G. Once the final shot has been fired the GTA unloads from point G to point I which is at 0.4 power factor leading. Once the firing cycle is complete the six-pulse rectifier disconnects from the main bus and the GTA returns to point E. When observing Figure 6-36 it becomes apparent that with the 5th harmonic attenuated all the GTA operating points lie within the thermal limits of both the field and stator windings and that the alternator is operating within its design capability curves. Hence, with the 5th harmonic attenuated no accelerated life consumption of the GTA is expected during continuous firing EM railgun operations.
6.5.5 Summary

This section has argued that attenuation of the 5th harmonic can significantly reduce the consequences of operating in the presence of harmonic waveform distortion, which were summarised in Table 6-6. This has been visually presented by means of Figure 6-35 and Figure 6-36 which demonstrate that with the 5th harmonic attenuated the resulting GTA operating points for both single shot and continuous firing lie within the thermal limits of both the field and stator windings and that the alternator is operating within its design capability curves. As such, attenuation of the 5th harmonic is considered to yield acceptable system performance when conducting EM railgun operations. Therefore, attenuation of higher order harmonics was not considered necessary. It was also argued that the 5th harmonic filter does not significantly impact on the transient performance of the warship electric power system during EM railgun operations. This was demonstrated by means of Figure 6-28.

An important finding resulting from the arguments presented in this section is that acceptable operation of the warship electric power system during EM railgun operations has been achieved with a maximum total harmonic waveform distortion content of 8.16%, as shown in Figure 6-27. While this is outside of the NATO STANAG 1008 limit of 5% this standard is not mandated at the HV bus. Hence, based on the results of this research it is recommended that for warship electric power system designs comprising EM railguns the THD at the main bus be limited to a maximum 8.16%. Adhering to the maximum 8.16% THD limit at
the main bus during EM railgun operations will ensure that the alternator operates within its capability curves, thus no accelerated life consumption is expected to result. Furthermore the electric power system should not suffer significantly decreased transmission efficiency due to increased copper and iron losses or the mal operation of equipment sensitive to power quality, when compared to operating with the 5th harmonic present. A summary of the advantages and disadvantages of mitigation option 2 is provided in Table 6-9.

### Table 6-9 Option 2 - Summary of advantages and disadvantages

<table>
<thead>
<tr>
<th>Advantages</th>
<th>Disadvantages</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reduced THD from a maximum of 26.76% to a maximum of 8.16% during EM railgun operations has been shown to mitigate the consequences of operating in the presence of harmonic waveform distortion to an acceptable level.</td>
<td>Additional equipment required which incurs additional cost and requires additional space.</td>
</tr>
<tr>
<td>Reduced copper losses in the GTA, transformer and power cables and reduced iron losses in the GTA and transformer reduces additional heating thus increases efficiency and life expectancy of the components when compared to operating with the 5th harmonic present.</td>
<td>Catastrophic failure of capacitors in harmonic filter is possible due to degradation over time.</td>
</tr>
<tr>
<td>Reduced the peak values of the GTA voltage and GTA current waveforms such that the peak values are approximately equal to the fundamental, thus minimising the impact on other electrical consumers sensitive to power quality.</td>
<td></td>
</tr>
<tr>
<td>Reducing the dV/dt and di/dt of the GTA voltage and current waveforms respectively reduces the stress on the insulation of the alternator, transformer and power cables which will increase life expectancy when compared to operating with the 5th harmonic present.</td>
<td></td>
</tr>
<tr>
<td>GTA operating points for both single shot and continuous firing lie within the current alternator design capability curves. As such, with 5th harmonic attenuated the stator winding temperature and the field winding temperature will remain within the limits of the current class of insulation thus accelerated life consumption is not expected.</td>
<td></td>
</tr>
</tbody>
</table>

### 6.6 Concluding arguments and system solution recommendation

The aim of this chapter was to develop meaningful discussion around practical applications which are useful to the naval design authority when integrating EM railguns with warship electric power systems using the system considered in this research. In achieving this aim this chapter has offered discussion and formulated arguments from a systems engineering science perspective based on the results presented in Chapter 5. Section 6.2 has discussed the time-varying transient impact on the RMS values of the GTA voltage and frequency resulting from EM railgun operations. Section 6.3 has discussed the impact of the harmonic waveform distortion present during the operation of the EM railgun as a result of the non-linear characteristics of the six-pulse thyristor rectifier employed to charge the ESD. In both cases a description of the results obtained in Chapter 5 has been offered and the implications and consequences for the design
of warship electric power systems discussed. Two approaches to mitigate the unacceptable consequences identified were then offered and the advantages and disadvantages discussed in each case.

6.6.1 Concluding arguments

As discussed in section 6.2 implications resulting from the transient impact of integrating EM railguns with warship electric power systems have unacceptable consequences for the electric power system, as summarised in Table 6-2. With regards to firing single shots it was argued in section 6.2 that to prevent the occurrence of under-voltage, under-frequency and over-frequency events, which may trigger protection relays (Bayliss, 1999) (Prousalidis & Kourtesis 2013 b) or exceed the material strengths of the GTA, the minimum ESD charge time and GTA unload time must be constrained to 8.25 s and 2.25 s respectively. This limits the minimum single EM railgun shot time to 10.50 s. Applying this limit prevents under-voltage, under-frequency and over-frequency events from interrupting EM railgun firing operations and helps to maintain the life expectancy of the GTA. As no minimum required single shot time has been defined in the literature, the minimum shot time suggested in this research is considered an acceptable limit and will be applied to the final solution.

With regards to continuous firing operations it was found that the minimum time between shots required to maintain the electric power system stability and to maintain the voltage and frequency transients within acceptable limits was 4.75 s. Reducing this minimum time between shots was found to induce under-frequency events which were found to have unacceptable consequences for the electric power system. It was also found that reducing the ESD capacity below the 305 MJ suggested for continuous firing operations would require modifications to the GTA load shed logic triggers to permit a larger GTA load shed to be taken than is currently allowed. This may have unacceptable consequences for the GTA. This was found to be the design driver for the ESD capacity required for continuous firing EM railgun operations. It was therefore recommended that the continuous rate of fire be limited to a maximum rate of 12 shots per minute and that an energy store of capacity 305 MJ be employed to maintain the life expectancy and safe operation of the GTA. The rate of fire to which it is recommended that continuous firing be constrained is commensurate with current requirements (Chaboki, et al., 2004) (McNab, 2007) (Osborn, 2015). Thus, imposing this limit is considered an acceptable mitigation and will be applied to the final solution. The suggested constraints to mitigate the transient impact of EM railguns on warship electric power systems result to the voltage and frequency transient elements of QPS being with acceptable limits as defined by NATO STANAG 1008 for both single shot and continuous firing scenarios. This was considered advantageous in that the naval design authority may design equipment which may be exposed to the transient impact of the EM railgun, such as the GTA and EM railgun support load, in the knowledge that the voltage and frequency excursions resulting from the operation of the EM railgun should not exceed the limits of NATO STANAG 1008.

As discussed in section 6.3 implications resulting from the harmonic impact of integrating EM railguns with warship electric power systems have unacceptable consequences for the warship electric power system. These consequences were summarised in Table 6-6. Thus, suitable mitigations must be implemented to prevent such consequences manifesting. It was argued in section 6.3 that when considering
Discussion

the consequences resulting from the harmonic impact on the warship electric power system it becomes apparent that the current electric power system design practice is not robust enough to withstand the rigours of EM railgun operations. This is perhaps most obviously demonstrated in Figure 6-14 and Figure 6-15 which clearly show the alternator operating outside of its design capability limits when providing power for the EM railgun in single shot and continuous firing scenarios respectively. The implications of this were summarised in Table 6-5 and discussed in section 6.3. As such, two options aimed at mitigating the consequences of harmonic waveform distortion were presented in section 6.3.

1. Increase the robustness of the warship electric power system such that the resulting harmonic waveform distortion does not adversely impact on the GTA, transformer, power cables or the protection system.

2. Reduce the resulting harmonic waveform distortion to a level where the impact on the GTA, transformer, power cables and protection system is considered acceptable.

It was argued in section 6.3 that from a systems engineering perspective option 1 had significant disadvantages. Firstly, in increasing the capability of the alternator it was demonstrated that the GT would only be able to drive the alternator at 75% of full load. This continuous part load operation would decrease the alternator efficiency across all operational scenarios. A second significant disadvantage of option 1 from a systems engineering perspective was that as this option makes no attempt to decrease the harmonic waveform distortion present during EM railgun operations the consequences of the higher than expected peak voltage and peak current values and the increased rate of rise of the voltage and current waveforms will continue to manifest. Hence, while the GTA, transformer and power cables can be rated to withstand the rigours of EM railgun operation, accelerated life consumption would still occur when compared with operating at the fundamental frequency, as would interference with the protection system. The disadvantages described here and in section 6.3 preclude the selection of option 1 for the final solution.

When considering option 2 it was argued that attenuation of the 5th harmonic can mitigate the consequences of operating in the presence of harmonic waveform distortion. These arguments were summarised in Table 6-9. The associated disadvantages were also discussed in section 6.3 and presented in Table 6-9. The impact of attenuating the 5th harmonic was visually presented by means of Figure 6-35 and Figure 6-36 which demonstrated that with the 5th harmonic attenuated the resulting GTA operating points for both single shot and continuous firing lie within the thermal limits of both the alternator field and stator windings. Thus, it was argued that with the 5th harmonic attenuated the alternator is operating within its design capability curves during both single shot and continuous firing EM railgun operations. Hence, it was concluded that attenuation of the 5th harmonic is considered to yield acceptable system performance when conducting EM railgun operations and that no accelerated life consumption of the GTA, transformer or power cables is expected. Further supporting the selection of option 2 it was argued that with the 5th harmonic attenuated the electric power system should not suffer significantly decreased transmission efficiency or the mal operation of the protection system. Hence, based on the arguments presented in section 6.2 and section 6.3 option 2 was selected for the final solution. An important finding from the discussion presented in section 6.3 was that the acceptable operation of the electric power system during EM railgun operations with
regards to component life expectancy, transmission efficiency and the correct operation of the protection system can be achieved with a maximum total harmonic waveform distortion content of 8.16% present at the main HV bus over a frequency range up to 3000 Hz, or the 50th harmonic. As such, a maximum THD limit of 8% at the main HV bus during EM railgun operations should be imposed on the final solution.

### 6.6.2 System solution recommendation

The system diagram for the final solution to integrate an EM railgun with a warship electric power system, based on the results of this research and the arguments presented in section 6.2 and section 6.3, is offered in Figure 6-37. The firing constraints which should be placed on each firing protocol along with the corresponding capacity of the ESD required to maintain the transient elements of QPS within acceptable limits as defined by NATO STANAG 1008 are summarised in Table 6-10. The maximum main HV bus THD limit during EM railgun operations is also given in Table 6-10. Adherence to the constraints and limits detailed in Table 6-10 ensures the acceptable operation of the warship electric power system during EM railgun operations.

![Figure 6-37 Warship electric power system comprising an EM railgun – Final solution](image)

**Table 6-10 EM railgun firing constraints and ESD capacity required to maintain acceptable QPS**

<table>
<thead>
<tr>
<th>Firing protocol</th>
<th>Firing constraint</th>
<th>ESD capacity suggested</th>
<th>THD limit at main HV bus</th>
</tr>
</thead>
<tbody>
<tr>
<td>Single shot</td>
<td>Minimum complete shot cycle time 10.50 s</td>
<td>160 MJ</td>
<td>8% &lt; 50th harmonic</td>
</tr>
<tr>
<td>Continuous</td>
<td>Shots per minute ≤ 12</td>
<td>305 MJ</td>
<td></td>
</tr>
</tbody>
</table>
Chapter 7 Conclusions and further work

7.1 Introduction

The question this research aimed to answer was; what are the implications and consequences of integrating an EM railgun with a warship electric power system and under what constraints should the EM railgun be operated to ensure an acceptable level of QPS to other electrical consumers during firing operations. Following an extensive literature review into the state-of-the-art in warship electric power system design and a critical review into integrating EM railguns with warship electric power systems, it was concluded that knowledge was lacking regarding the impact of EM railguns on candidate warship electric power system QPS. Two key ways in which this field could be advanced were identified. The first was the requirement for a validated model with which to explore the transient and harmonic impact of EM railgun operations on the QPS of a candidate warship electric power system. The second was the design and development of an EM railgun specific ESD and charging control system, capable of facilitating a continuous rate of fire of 12 rounds per minute. Both of these key aspects were addressed in this thesis prior to the commencement of the investigations.

To conduct research into this field a candidate warship electric power system was identified based on the requirements of a future surface combatant. A suitable circuit with which to investigate the impact of EM railguns on the candidate warship electric power system was also selected, justified and described. A six-pulse thyristor rectifier was selected to control the rate of charge of the ESD. This particular power converter was selected because thyristors are available as high power robust devices meaning the resulting power converter has lower losses when compared to other types of power electronic converters. This type of rectifier is simple to control and proved capable of facilitating the required rates of fire. The six-pulse thyristor rectifier was connected in series with an isolation transformer and large GTA. The resulting circuit was used to charge the capacitive EM railgun ESD. A theoretical analysis of this circuit was then offered which described the interaction between the EM railgun and the warship electric power system from a mathematical perspective. This developed an analytical and conceptual understanding of the transient and harmonic impact of EM railguns on the QPS of a candidate warship electric power system. To examine and analyse the electric power system transients across the time period pertaining to the operation of the EM
railgun, a time-domain simulation tool was developed, based on the mathematical analysis of the circuit under investigation. The justification of the model parameters were offered and the model limitations were described. To ensure that the results of the simulations conducted were suitably representative of the candidate warship electric power system under investigation, a thorough validation and verification process was conducted. It was concluded that the model was suitable for use in this research thus assuring the credibility and usefulness of the results to the naval design authority.

The model was then employed to explore the transient and harmonic impact of EM railguns on warship electric power system QPS when conducting EM railgun firing operations commensurate with the requirements of future surface combatants. The justification for, and a description of, the investigations undertaken was given before the results and observations were presented. The results were described with reference to the analytical description of the circuit under investigation presented in Chapter 3. Following the presentation of the results the implications and consequences of the transient and harmonic impact on QPS resulting from EM railgun operations, for the design of warship electric power systems, were discussed. Owing to the difference in analysis between the time-varying transient elements of QPS, being the voltage and frequency transients, and the repeatable periodic elements of QPS, being the harmonic waveform distortion, the impacts were discussed separately. Mitigations to combat the unacceptable consequences identified were then offered, with the advantages and disadvantages being discussed in each case. Finally, a summary of the arguments justifying the selection of a final system solution to integrate an EM railgun with a warship electric power system was provided.

The following sections provide a summary of the research findings and specific recommendations for the naval design authority when integrating EM railguns with warship electric power systems. The findings and recommendations have been contributed as a result of this research. Areas for future work required to enable a more complete understanding of the research problem considered are also suggested.

7.2 Summary of the research findings

A key finding of this research is that currently, warship electric power system design practice is not robust enough to withstand the rigours of EM railgun operations and that reduced efficiency, reduced life expectancy and the mal operation of electrical loads sensitive to power quality may manifest. This is a result of the harmonic waveform distortion introduced by the six-pulse thyristor rectifier employed to draw and store energy provided by the GTA in a capacitive ESD, prior to it being supplied to the EM railgun via a PFN. Furthermore, it was found that unless limits are placed on the rate of EM railgun firing under-voltage, under-frequency and over-voltage events may occur which may trigger the protection relays or exceed the material strengths of the GTA. These findings suggest that without suitable design mitigations the integration of an EM railgun with a warship electric power system may have unacceptable consequences for the warship electric power system. It could be argued that a different converter could be used but from the literature survey conducted the six-pulse thyristor rectifier was considered the most suitable.
With regards to firing single shots it was found that to prevent the occurrence of under-voltage, under-frequency and over-frequency events which may trigger the protection relays or exceed the material strengths of the GTA the minimum ESD charge time and GTA unload time must be constrained to 8.25 s and 2.25 s respectively. This limits the minimum single EM railgun shot time to 10.50 s. For the case of single shot EM railgun firing the ESD capacity was found to be driven by the energy required per shot which is 160 MJ. With regards to continuous firing operations it was found that to prevent the occurrence of under-frequency events and to maintain the electric power system stability the minimum time between shots should be limited to 4.75 s. It was also found that reducing the ESD capacity below 305 MJ for continuous firing operations would require modifications to the GTA load shed logic triggers to permit a larger GTA load shed to be taken than is currently allowed. This would have unacceptable consequences for the GTA from a life expectancy and safety perspective if the resulting over-speed exceeded the material strengths of the GTA.

The consequences of adjusting the GTA load shed triggers was found to be the design driver for the ESD capacity required for continuous firing EM railgun operations. The suggested constraints to mitigate the previously described transient impact of EM railguns on warship electric power systems were found to result to the voltage and frequency transient elements of QPS being with acceptable limits as defined by NATO STANAG 1008. This was considered advantageous because it is likely that the EM railgun support load, which will be subject to the voltage and frequency transients resulting from EM railgun operations, will be designed to operate within the constraints of NATO STANAG 1008. This is because it is an LV load for which the constraints of NATO STANAG 1008 are mandated. As such, the naval design authority may design such loads in the knowledge that the voltage and frequency transients to which they may be exposed as a result of EM railgun operations will be within the NATO STANAG 1008 limits. This was found to be the case for both single shot and continuous firing scenarios.

For the case of both single shot and continuous firing EM railgun operations the resulting harmonic waveform distortion was found to have significant consequences for the warship electric power system. It was found that the levels of harmonic waveform distortion present during EM railgun operations may cause additional heating in the GTA, transformer and power cables due to increased copper losses and cause additional heating in the GTA and transformer through increased iron losses. The consequences of the increased heating were found to be the reduced efficiency of electric power transmission and the reduced life expectancy of the GTA, transformer and power cables due to increased thermal stress on the insulation. It was also found that the harmonic waveform distortion present during EM railgun operations may cause the mal operation of the protection system. As such, two options aimed at mitigating the consequences of harmonic waveform distortion resulting from EM railgun operations were presented:

1. Increase the robustness of the warship electric power system such that the resulting harmonic waveform distortion does not adversely impact on the GTA, transformer, power cables or the protection system.

2. Reduce the resulting harmonic waveform distortion to a level where the impact on the GTA, transformer, power cables and protection system is considered acceptable.
It was found that option 1 had significant disadvantages which precluded selection for a warship electric power system. These disadvantages included decreased alternator efficiency across all operational scenarios and the continued manifestation of consequences associated with higher than expected peak voltages, higher than expected peak currents, increased $dV/dt$ and increased $di/dt$.

Option 2 was found to successfully mitigate the consequences of operating in the presence of harmonic waveform distortion such that the impact on the GTA, transformer, power cables and protection system was considered acceptable during EM railgun operations. Acceptable performance was considered to mean that during the operation of the EM railgun the electric power system did not suffer significantly reduced transmission efficiency; that the GTA, transformer and power cables did not suffer significantly accelerated life consumption; and that the mal operation of the protection system should not manifest. A key finding of this research was that the acceptable operation of the electric power system during EM railgun operations can achieve with a maximum total harmonic waveform distortion content of 8.16% present at the HV bus over a frequency range up to 3000 Hz, or the 50th harmonic. For the case of the candidate warship electric power system considered in this research the total harmonic waveform distortion content was limited to 8.16% at the HV bus through the installation of a 17.5 MVAr 5th harmonic filter in parallel with the six-pulse thyristor rectifier used to charge the EM railgun ESD. It was found that with the 5th harmonic attenuated the GTA operating points for both single shot and continuous firing operations lie within the thermal limits of both the field and stator windings and that alternator is operating within its design capability curves during both EM railgun firing scenarios. These findings suggest that if the total harmonic waveform distortion content at the main HV bus is limited to 8.16%, or practically 8%, the electric power system should not suffer significantly decreased life expectancy, significantly decreased transmission efficiency or the mal operation of the protection system during EM railgun operations.

The findings of this research have been translated into specific recommendations for the naval design authority when integrating an EM railgun with a warship electric power system. These recommendations are presented in the following section.

### 7.3 Recommendations for the naval design authority

Using the system proposed the findings from this research have contributed the following recommendations for the naval design authority when integrating EM railguns with warship electric power systems. The aim of the recommendations is to provide guidance such that the integration of an EM railgun with a warship electric power system results in acceptable electric power system performance during EM railgun firing operations. Hence, the adoption of the recommendations offered here ensures that the warship electric power system should not suffer significantly decreased life expectancy, significantly decreased transmission efficiency or the mal operation of the protection system during EM railgun operations. The resulting recommendations are as follows:

1. When operating under single shot protocol 160 MJ of energy storage should be utilised to store the energy required per shot. The minimum ESD charge time and GTA unload time must be
constrained to 8.25 s and 2.25 s respectively. This limits the minimum single EM railgun shot time to 10.50 s.

2. When operating under continuous firing protocol 305 MJ of energy storage should be utilised to store the 160 MJ required per shot and store the residual energy required to reduce the GTA load transient to an acceptable level. The continuous rate of fire should be limited to a maximum rate of 12 shots per minute.

3. The maximum THD present at the HV bus during EM railgun operations should be limited to 8% over a frequency range up to 3000 Hz, or the 50th harmonic.

7.4 Recommendations for further work

Throughout this research three key areas of further work required to provide the naval design authority with a more complete understanding of the problem investigated have been identified. Hence, further work recommendations are described in the following sections. In each case, any extra resources required are detailed.

7.4.1 Modification of GTA load shed logic

The GTA model employed in this research has an inbuilt load shed logic feature designed to manage the GTA fuel flow during excessive GTA load sheds. This limits the maximum permissible load shed with this GTA model to 13 MW. The aim of the load shed logic feature is to prevent excessive over-speed. Therefore, adjusting the load shed triggers may result in increased GTA over-speed, which may cause damage or accelerated life consumption. However, the extent of the resulting over-speed could not be investigated with the model employed in this research. This GTA load shed limit was found to drive the capacity of the required ESD. Hence, it is recommended that further work be conducted to investigate the impact of modifying the GTA load shed logic triggers to allow the GTA to take increased load sheds. The results from this further work can be used to assess the extent to which the capacity of the ESD may be reduced whilst maintaining an acceptable standard of QPS with regards to the life expectancy and safe operation of the GTA. This further work could be completed with the current model however, elevated permissions from Rolls-Royce to make modifications to the GTA load shed logic that are not currently permitted would be required.

7.4.2 Mechanical and thermal impact on the GTA during EM railgun firing

It was stated in section 4.3.3 that the mechanical and thermal impact on the GTA during EM railgun operations was outside the scope of this research. As such, the conclusions drawn on the transient and harmonic impact of EM railgun operations on QPS are limited to the electrical perspective only and do not provide a guarantee of the GTA performance from a mechanical or thermal standpoint. However, as discussed in section 6.2 and as is demonstrated by the results presented in Chapter 5, during continuous
EM railgun firing operations the GTA frequency is in a transient state. Thus, it is recommended that the mechanical impact of the repeated acceleration and deceleration of the GTA, corresponding to the GTA frequency transients during EM railgun operations, be investigated to assess the impact on the life expectancy of the GTA. While this model can provide the periodic rotational acceleration and deceleration of the GTA when providing power for the EM railgun further information on the mechanical limitations of the GTA components would be required to complete an assessment of the impact on the GTA from a mechanical perspective.

7.4.3 Further develop ESD charging bridge controller

Thus far, this research has proposed and proven an elementary circuit to control the rate of charge of the EM railgun Energy Storage Device (ESD). The ESD charging control system has been proven capable of facilitating a maximum rate of fire of 12 shots per minute, which is commensurate with the requirements of future surface combatants. While the elementary nature of this controller has proved sufficient to conduct this research, further development is required in two key areas:

1. As shown in Figure 5-9 and Figure 5-25 there is visible noise on the firing angle delay signal used to control the ESD charging current. This means that the firing delay angle is rapidly changing which manifests as noise on the main bus THD plot, as shown in Figure 5-9 and Figure 5-25. This noise is due to the ripple on the ESD capacitor voltage feedback signal which could be removed by filtering this signal before it is passed to the control loop. This would smooth the switching control of the thyristors which may reduce the harmonic waveform distortion and improve the stability of the controller.

2. As described in section 4.5.3 a recognised limitation of the controller developed in this research is that it can only execute pre-defined shot patterns at a continuous rate of fire. A further implication of this limitation is that currently, the controller cannot respond to interrupts. Such interrupts may include the operator not wanting to fire the shot once the ESD is charged or the operator cancelling the shot mid-way through the ESD charging cycle, both of which would require the safe unload of the GTA and the management of the energy already stored in the ESD. Hence, before being practically deployable the controller should undergo further development such that interrupts can be handled.

7.4.4 Explore alternative power converters to charge the EM railgun ESD

The power converter selected to control the charge of the ESD in the case of this research was the six-pulse thyristor rectifier. As described in section 3.4 this type of power converter was selected for its high efficiency, high power density and relative ease of control. However, as the results of this research have demonstrated this type of power converter introduces significant harmonic waveform distortion into the electric power system which as described in section 6.3.7, has numerous unacceptable consequences. As such, heavy filtering is required to reduce the harmonic waveform distortion to an acceptable level. As described in section 2.2.2 PWM converters have a superior harmonic performance when compared with
Conclusions and further work

thyristor rectifiers due to their ability to draw near sinusoidal current from the GTA (Evans & Hoevenaars, 2007). Furthermore, IGCTs are now available with similar power ratings to thyristors and can be employed in PWM converter topologies due to their forced turn-off capability. However, PWM switching control is more complex and the very high frequency switching required to draw near sinusoidal current from the GTA introduces significant switching losses which reduces the efficiency. Therefore, a PWM converter composed of IGCT power electronic devices capable of a high switching frequency may deliver a superior harmonic performance such that the requirement for heavy harmonic filtering during EM railgun operations is negated, thus saving cost and space. Hence, the challenges associated with implementing a PWM IGCT converter to charge the ESD should be explored to determine if an alternative superior power converter is practically realisable.
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Appendix A  Six-pulse thyristor rectifier waveforms

Figure A-1 shows the three phase AC supply waveforms and the DC output voltage of the six-pulse thyristor rectifier in the upper plot. The DC output voltage is shown as the black line in the upper plot. In this case, the firing delay angle is 0 degrees. As shown in the second, third and fourth plots the AC input current waveforms are quasi-square waves comprising harmonic waveform distortion.

Figure A-2 shows the three phase AC supply waveforms and the DC output voltage of the six-pulse thyristor rectifier in the upper plot. The DC output voltage is shown as the black line in the upper plot. In this case, the firing delay angle is 45 degrees. As shown in the second, third and fourth plots the AC input current waveforms are quasi-square waves which are badly distorted and are phase shifted 45 degrees with respect to the AC input voltage waveforms.
Figure A-1 Relationship between the three phase AC supply voltage waveforms, DC output voltage and the AC supply current waveforms for a six-pulse thyristor rectifier – Firing angle 0 degrees
Figure A-2 Relationship between the three phase AC supply voltage waveforms, DC output voltage and the AC supply current waveforms for a six-pulse thyristor rectifier – Firing angle 45 degrees
Appendix B  Software selection

The software package selected in which to build the modelling tool was MATLAB®/Simulink®, together with the power systems blockset library SimPowerSystems, which facilitates the simulation of electrical power system components. While other options such as Power Systems Computer Aided Design (PSCAD) software were considered, MATLAB®/Simulink® was selected for the following reasons.

To facilitate the integration of existing models
In supporting this research, a model of the Rolls-Royce MT30 GT was supplied by Rolls-Royce for integration into the modelling tool, a further explanation of which is given in section 4.4.1. This GT is currently installed a candidate electric warship likely to field EM railguns (LaGrone, 2015) thus increasing the relevance and usefulness of the results of this research. The model was supplied as a MATLAB®/Simulink® s-function, which meant any deviation from the MATLAB®/Simulink® simulation environment would have constituted a re-build and re-validation of the GT model. To protect the intellectual property of the MT30 GT the model was supplied as a black box and as such, any attempt to replicate the model would have contravened the agreement under which the model was supplied. Thus, the desire to integrate this model was a major factor in selecting MATLAB®/Simulink® in which to perform this simulation based research.

To provide continuity with previous works in the research field
Much of the work previously conducted in this field utilises MATLAB®/Simulink® and SimPowerSystems in which to conduct simulation based research (Sudhoff, et al., 2004) (Lewis, 2006) (Kanellos, et al., 2007) (Tsekouras, et al., 2010) (Kanellos, et al., 2011). Maintaining consistency with the software packages previously employed allows for good comparison of the models used and the modelling techniques employed.

To align with the industry standard
Recently, efforts have been made to standardise the approach taken towards the modelling of marine electrical power systems in the naval domain (Deverill, et al., 2003) (van der Burgt, et al., 2004 b) (Bennett, et al., 2007). Much of this work has been focused on the development of modelling standards and on the development of a library of marine power systems components based on MATLAB®/Simulink®.
Appendix C  Simulink model schematics

The following appendix contains each of the Simulink® model schematics as built in the Simulink modelling environment. Each of the models described in this appendix are described fully in Chapter 4.

Figure C-3 shows the performance tool top level Simulink® model schematic. A full description of this model can be found at section 4.3.

The model demonstrates the component parts which comprise the overall system model. Shown in Figure C-3 are the models of the GTA, 2 MW EM railgun support load, generator cable, isolation transformer, EM railgun cable, thyristor charging bridge, charging control system and the ESD.
Figure C-4 shows the GT and governor Simulink® model schematic as supplied by Rolls-Royce. A full description of this model can be found at section 4.4.1.

Shown in Figure C-4 is the GT engine and governor model, which is the central block labelled MT30_GT1. This was supplied as a black box model as described at Appendix E. Also shown is the input bus and the load model. The pulse generator input to the GT engine model triggers the execution of the model independently of the overall simulation time step, which enables replication of the actual GT governor controller sample time.
Figure C-5 shows the GTA Simulink® model schematic following the modifications required for employment in this research. A full description of this model can be found at section 4.4.1.

As shown in Figure C-5 the load model shown in Figure C-4 has been replaced with a synchronous alternator model. This is shown in the top right of the model labelled as ALTERNATOR. The speed feedback loop into the GT model is also shown.
Figure C-6 Synchronous machine Simulink® model schematic

Figure C-6 shows the synchronous alternator Simulink® model schematic labelled as Synchronous machine pu Standard. A full description of this model can be found at section 4.4.2.

The input to the synchronous alternator model is the GT engine model output power labelled as DELP_PU. Also shown is the speed feedback loop and the AVR which is labelled as IEEE AVR Type 2.
Figure C-7 Synchronous generator mechanical part Simulink® model schematic

Figure C-7 shows the mechanical part of the synchronous generator Simulink® model schematic. A full description of this model can be found at section 4.4.2. The inputs to the model are the mechanical power (Pm) from which the mechanical torque (Tm) is calculated through division by rotational speed. The model outputs the resulting mechanical rotor speed.

Figure C-8 AVR Simulink® model schematic

Figure C-8 shows the AVR Simulink® model schematic. A full description of this model can be found at section 4.4.3.

The inputs to the model are the reference voltage (Vref), the generator terminal voltage (Vt), the generator terminal current (It) and the exciter field current (Ifd). The model output is the voltage applied to the generator field winding (Efd).
Figure C-9 EM railgun ESD charging control system Simulink® model schematic

Figure C-9 shows the ESD charging control system Simulink® model schematic. A full description of this model can be found at section 4.5. In addition to the ESD charging circuit controller the pulse generator and the PLL are also shown.

Figure C-10 EM railgun ESD Simulink® model schematic

Figure C-10 shows the EM railgun ESD Simulink® model schematic. A full description of this model can be found at section 4.6.

Shown in the model is the ESD capacitor with SR and the braking resistor. Also shown are the circuit breakers to switch the ESD and the braking resistor in and out of the circuit and the corresponding breaker control logic. The voltage and current measurements, used to determine the voltage of the capacitor and the GTA energy supplied during EM railgun firing are also shown.
Appendix D  Synchronous alternator model equations

The following equations are those with which SimPowerSystems blockset calculates the voltage and flux linkage quantities when executing the Synchronous Machine model, as described in section 4.4.2. The voltage quantities are calculated using the following set of equations:

\[
V_d = R_s i_d + \frac{d\psi_d}{dt} - \omega_r \psi_q \tag{E78}
\]
\[
V_q = R_s i_q + \frac{d\psi_q}{dt} - \omega_r \psi_d \tag{E79}
\]
\[
e_{fd} = R_{fd} i_d + \frac{d\psi_{fd}}{dt} \tag{E80}
\]
\[
\psi_{1d}' = R_{1d}' i_{1d}' + \frac{d\psi_{1d}'}{dt} \tag{E81}
\]
\[
\psi_{1q}' = R_{1q}' i_{1q}' + \frac{d\psi_{1q}'}{dt} \tag{E82}
\]
\[
\psi_{2d}' = R_{2d}' i_{2d}' + \frac{d\psi_{2d}'}{dt} \tag{E83}
\]

And the flux linkage quantities are calculated using the following set of equations:

\[
\psi_d = L_i d + L_{ad}(i_d' + i_{1d}') \tag{E84}
\]
\[
\psi_q = L_i q + L_{aq} i_{1q} \tag{E85}
\]
\[
\psi_{fd}' = L_{fd}' i_d' + L_{md}(i_d + i_{1d}') \tag{E86}
\]
\[
\psi_{1d}' = L_{1d}' i_{1d}' + L_{md}(i_d + i_{1d}') \tag{E87}
\]
\[
\psi_{1q}' = L_{1q}' i_{1q}' + L_{mq} i_{1q} \tag{E88}
\]
\[
\psi_{2q}' = L_{2q}' i_{2q}' + L_{mq} i_{2q} \tag{E89}
\]
Appendix E  GT and load model validation
confirmation letter from Rolls-Royce

The following appendix contains the GT and load model validation confirmation letter from Rolls-Royce.

The key points are summarised as follows:

1. The MT30 GT model supplied to Ian Whitelegg by Rolls-Royce was validated against actual test bed results involving typical load steps and one case of a full loss of electrical load from 36 MW which confirmed a high level of model to test bed agreement, across the tests conducted.

2. The MT30 GT model governor consists of a PI controller. The parameter FC_KNL1 is the GT governor proportional gain and FC_KNL2 is the GT governor integral gain. The GT governor parameters are fixed within the GT engine and are as supplied and tuned by Rolls-Royce.

3. The GT and governor model supplied by Rolls-Royce contains an inbuilt load shed logic feature. A load shed is considered a sudden loss of load occurring when the GT is operating at mid to high power which results in excessive GT over-speed. An essential control requirement during such a load shed is that the GT does not excessively over-speed and become unstable. As such, if a load shed occurs at mid to high power the control system takes emergency action to temporarily reduce the fuel flow while ensuring the engine does not flame out. The load shed logic is a safety critical system and cannot be disabled in the MT30 GT model provided to Ian Whitelegg.
To whom it may concern

Dear Sir/Madam,

I can confirm that Rolls-Royce have supplied Ian Whitelegg with a model of the Rolls-Royce MT30 Gas Turbine (GT) engine which to conduct his PhD research. The model was supplied as a non-transparent C-code compiled Simulink model, wrapped in an S Function. I can confirm the following points regarding the model.

- The MT30 GT model supplied to Ian Whitelegg by Rolls-Royce was validated against actual test bed results involving typical load steps and one case of a full loss of electrical load from 36 MW, which confirmed a high level of model to test bed agreement, across the tests conducted.

- The MT30 GT model governor consists of a PI controller. The parameter FC_KNL1 is the GT governor proportional gain and FC_KNL2 is the GT governor integral gain. The GT governor parameters are fixed within the GT engine and are as supplied and tuned by Rolls-Royce.

- The GT and governor model supplied by Rolls-Royce contains an inbuilt load shed logic feature. A load shed is considered a sudden loss of load occurring when the GT is operating at mid to high power which results in excessive GT over-speed. An essential control requirement during such a load shed is that the GT does not excessively over-speed and become unstable. As such, if a load shed occurs at mid to high power the control system takes emergency action to temporarily reduce the fuel flow while ensuring the engine does not flame out. The load shed logic is a safety critical system and cannot be disabled in the MT30 GT model provided to Ian Whitelegg.

Yours faithfully,

B T Thorp
General Manager
Naval EA&C
### Appendix F  Synchronous alternator data sheet

#### ELECTRICAL DATA SHEET

**Falcon Works, Nottingham Road, Loughborough, Leics, LE11 1EX, England**  
**Telephone: 14(0) 1509 3131 Fax: 14(0) 1509 3136**  
**E-mail: network@brush.co.uk**

1. **RATING DETAILS**
   - **Frame size**: DDAK 7-200ERH  
   - **Terminal voltage**: 11.00 kV  
   - **Frequency**: 50 Hz  
   - **Speed**: 3000 rev/min  
   - **Power factor**: 0.800  
   - **Applicable national standard**: IEC 60034-3  
   - **Rated coolant inlet temperature**: 40.0 °C  
   - **Rated output**: 30.070MVA, 45.000 MVA

2. **PERFORMANCE CURVES**
   - **Output vs coolant inlet temperature**: H.E.P. 34004  
   - **Generator capability diagram**: H.E.P. 34005  
   - **Efficiency vs output**: H.E.P. 32390  
   - **Open and short circuit curves**: H.E.P. 32385  
   - **Permitted duration of negative sequence current**: H.E.P. 1216

3. **REACTANCES**
   - **Direct axis synchronous reactance, Xd(II)**: 179 %  
   - **Direct axis saturated transient reactance, Xd(IV)**: 16.7 % ± 15 %  
   - **Direct axis saturated sub transient reactance, Xd(IV)**: 12.0 % ± 15 %  
   - **Unsaturated negative sequence reactance, X2(II)**: 14.8 %  
   - **Unsaturated zero sequence reactance, X0(II)**: 7.8 %  
   - **Quadrature axis synchronous reactance, Xq(II)**: 176 %  
   - **Quadrature axis saturated transient reactance, Xq(IV)**: 18 %  
   - **Quadrature axis saturated sub transient reactance, Xq(IV)**: 15 %  
   - **Short circuit ratio**: 0.58

**Notes:**

- The electrical details provided are calculated values. Unless otherwise stated, all values are subject to tolerances as given in the relevant national standards.

**Date:** 04-Jun-2014  
**I.D.:** OPP03883C1

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Figure F-11 Synchronous alternator data sheet page 1
4. RESISTANCES AT 20°C
   4.1 Rotor resistance 0.140 ohms
   4.2 Stator resistance per phase 0.0053 ohms

5. TIME CONSTANTS AT 20°C
   5.1 Transient O.C. time constant, $T_{ol}$ 0.6 seconds
   5.2 Transient S.C. time constant, $T_e$ 0.05 seconds
   5.3 Sub transient O.C. time constant $T_{ol}$ 0.05 seconds
   5.4 Sub transient S.C. time constant $T_e$ 0.04 seconds

6. INERTIA
   6.1 Moment of inertia, $WR^2$ (See note 2) 970 Kg.m²
   6.2 Inertia constant, $H$ 1.06 kW.seconds/kVA

7. CAPACITANCE
   7.1 Capacitance per phase of stator winding to earth 0.16 microfarad

8. EXCITATION
   8.1 Excitation current at no load, rated voltage 303 amps
   8.2 Excitation voltage at no load, rated voltage 42 volts
   8.3 Excitation current at rated load and P.F. 785 amps
   8.4 Excitation voltage at rated load and P.F. 191 volts
   8.5 Inherent voltage regulation, F.L. to N.L. 98 %

Notes:
1. The electrical data provided are calculated values.
   Unless otherwise stated, all values are subject to tolerances as given in the relevant national standards.
   Date: 04-Jun-2014
   I.D.: OPP93893C1
2. The rotor inertia value may vary slightly with generator / turbine interface. In the event of conflict, the figure quoted on the rotor geometry drawing takes precedence.
   Page: 2 of 2

Figure F-12 Synchronous alternator data sheet page 2
Figure F-13 Synchronous alternator data sheet page 3
Figure F-14 Synchronous alternator data sheet page 4
Figure F-15 Synchronous alternator data sheet page 5
Appendix G  Cable model parameter calculations

The following appendix presents the calculations for determining the parameters of both the GTA cable and the EM railgun cable. The cable characteristics as provided by NexansAmerCable (2015) are summarised below in Table G-1.

Table G-1 Cable parameters as provided by NexansAmerCable (2015)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Size AWG/kcmil mm²</td>
<td>535</td>
</tr>
<tr>
<td>AC resistance at 90°C, 60 Hz Ω/1000 ft</td>
<td>0.033</td>
</tr>
<tr>
<td>Inductive reactance Ω/1000 ft</td>
<td>0.031</td>
</tr>
<tr>
<td>Current carrying capability per cable</td>
<td>528 Amps</td>
</tr>
</tbody>
</table>

As the parameters provided by NexansAmerCable (2015) are given in Ω/1000 ft a conversion into Ω/50 m was made using the following method:

\[
50 \text{ m} = 164 \text{ ft}
\]

And

\[
\frac{1000 \text{ ft}}{6.10} = 164 \text{ ft}
\]

Therefore,

\[
AC \text{ resistance} = \frac{0.033}{6.10} = 0.0054 \Omega/50 \text{ m}
\]

And
Inductive reactance \( (X_L) = \frac{0.031}{6.10} = j0.0051 \Omega/50 \text{ m} \)

Considering that

\[ X_L = 2\pi fL \quad \text{[E90]} \]

Then

\[ L = \frac{X_L}{2\pi f} = \frac{0.0051}{2\pi \times 60} = 13.50 \mu H/50 \text{ m} \]

Next, the rated generator current is calculated to determine current carrying capacity of cables

\[ \text{Rated MW} = \sqrt{3}(V_L)(I_L)(pf) \quad \text{[E91]} \]

Thus

\[ I_L = \frac{45 \times 10^6}{(11000)(\sqrt{3})} = 2361 \text{ A} \]

The generator is star connected therefore line current is equal to phase current. At a current carrying capability of 528 amps per cable (NexansAmerCable, 2015) 5 cables are required.

Total resistance per phase based on 5 cables in parallel for a 50 m cable:

\[ \left[ \left( \frac{1}{0.0054} \right)^5 \right]^{-1} = 1.08 \text{ m}\Omega \]

Total inductance per phase based on 5 cables in parallel for a 50 m cable:

\[ \left[ \left( \frac{1}{13.5 \times 10^{-6}} \right)^5 \right]^{-1} = 2.70 \text{ uH} \]
Appendix H  GTA model validation

The plots presented in this appendix demonstrate the validation process of the GTA model and support the results presented in section 4.8.

GTA governor response validation – supporting plots

Figure H-16 GTA model governor response validation – Step load developed power comparison
GTA model validation

The upper plot of Figure H-16 shows a comparison between the MT30 GT model developed power and the performance tool GTA model developed power to an increasing load step demand. The corresponding % error between the two is shown in the lower plot. The results were recorded in Table 4-7.

Figure H-17 GTA model governor response validation – Step load speed comparison

The upper plot of Figure H-17 shows a comparison between the MT30 GT model speed and the performance tool GTA model speed, to an increasing load step demand. The load step demand is shown in Figure H-16. The corresponding % error between the two is shown in the lower plot. The results were recorded in Table 4-7.
The upper plot of Figure H-18 shows a comparison between the MT30 GT model developed power and the performance tool GTA model developed power to a ramp load demand. The corresponding % error between the two is shown in the lower plot. The results were recorded in Table 4-7.
Figure H-19 GTA model governor response validation – Ramp load speed comparison

The upper plot of Figure H-19 shows a comparison between the MT30 GT model speed and the performance tool GTA model speed to a ramp load demand. The ramp load demand is shown in Figure H-18. The corresponding % error between the two is shown in the lower plot. The results were recorded in Table 4-7.
GTA AVR response validation – supporting plots

**Figure H-20 MT30 GTA AVR validation tests 1 and 2 results**

**Figure H-21 MT30 GTA AVR validation tests 3 and 4 results**

Figure H-20 and Figure H-21 show the GTA voltage following a rejection of 25% load at 0.8 PF from 25%, 50%, 75% and 100% load. In each case the GTA power is shown in the upper plot and the GTA voltage is shown in the lower plot. The results were recorded in Table 4-8.
Appendix I  Performance tool user guide

The following appendix contains a brief overview of the performance tools operation to equip the reader with an understanding of how the performance tool has been used to conduct this research. The MATLAB code setup file used to initialise the performance tool is also included.

Performance tool operation guide
The following section describes the operation of the performance tool described in Chapter 4. This section does not intend to provide an in depth user guide for the performance tool, but rather intends to furnish the reader with an understanding of how the performance tool has been used to conduct this research.

The tool is initialised via a setup script file, the function of which is twofold. Firstly, the setup file pre-loads the model initial conditions based on steady state operation with a base load of 2 MW on the GTA. Secondly, the user inputs the simulation variables under investigation, such as ESD charge time, into the setup script file. Based on the input variables further model variables required to run the requested test case are then calculated. A selection of key input variables, calculated variables and a description of their functions are given below in Table I-2. Once the variables have been selected the setup file is executed and the corresponding variables are loaded into the Simulink model components. The Simulink model is then run and the required outputs recorded.
Table 1-2 Setup script file variables and description

<table>
<thead>
<tr>
<th>Variable</th>
<th>Units</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ss_t</td>
<td>s</td>
<td>Charging sequence start time. This is time to allow the simulation initialisation transients to settle and for the system to be at steady state before the test case commences.</td>
</tr>
<tr>
<td>Ic_t</td>
<td>s</td>
<td>Initial charge time. This is the time taken to charge the ESD from empty to the required level of charge before the first shot is taken.</td>
</tr>
<tr>
<td>C_V_set</td>
<td>V</td>
<td>Capacitor charge voltage set point.</td>
</tr>
<tr>
<td>C_V_as</td>
<td>V</td>
<td>Capacitor voltage after first shot. This sets how much residual charge will remain in the capacitor following each shot. This parameter is used when conducting continuous firing operations.</td>
</tr>
<tr>
<td>T_btw_s</td>
<td>s</td>
<td>Time between shots. This defines the time between shots when conducting continuous firing operations.</td>
</tr>
<tr>
<td>Unl_t</td>
<td>s</td>
<td>Unload time. This is the time taken to unload back to the 2 MW EM railgun support load following the final shot of any operation.</td>
</tr>
<tr>
<td>Cap_C</td>
<td>F</td>
<td>This parameter defines the capacity of the ESD capacitor.</td>
</tr>
<tr>
<td>Cap_SR</td>
<td>Ω</td>
<td>This parameter defines the SR of the ESD capacitor.</td>
</tr>
</tbody>
</table>

Setup script file calculated variables

<table>
<thead>
<tr>
<th>Ir_Lim</th>
<th>V/s</th>
<th>Initial charge rate limit. This parameter limits the rate at which the capacitor charge voltage set point is allowed to increase.</th>
</tr>
</thead>
<tbody>
<tr>
<td>T_btw_Lim</td>
<td>V/s</td>
<td>Time between shots limit. This parameter limits the rate of ESD recharge between shots. This variable is used when conducting continuous firing operations.</td>
</tr>
</tbody>
</table>

Performance tool setup script

```
%% MT30_EM_Railgun_Ver_2 - Setup File

% Rolls Royce Marine Controls
% CVF plant and controller model

% Author S.P.Tomlinson - December 2006
% Version controlled by Sourcesafe

%% Version modified by Ian Whitelegg - University College London - March 2015

% Edit adds the capability to model the MT30 GTA under the loading
% indicative of an EM railgun when firing in single shot or salvo mode

%% Load initial conditions

% 2 MW EM railgun support load initial condition
load GT1_2MW.mat

%% DEFINE EM GUN OPERATION CHARACTERISTICS

%%%%-------- INPUT PARAMETERS %%%%%

Ss_t = 5 % (Seconds) % Sequence start time - set to 5 seconds to allow simulation start transients to stabilise
Ic_t = 8.75 % (Seconds) % Initial charge time - time taken to charge ESD from empty to full charge
```

267
C_V_set = 11000 % (Volts) % Capacitor charge voltage setpoint
C_V_as = 7778 % (Volts) % Capacitor voltage after first shot (set by how much residual charge is required between shots to limit MT30 GTA transients)
T_btw_s = 6 % (Seconds) % Time between shots
Unl_t = 4.25 % (Seconds) % Time taken to unload back to 2 MW base load
Unl_res = 4.5 % (Ohms) % Size of unload resistor (2.75)
Cap_C = 2.646 % (Farads) % Size of ESD capacitor (5.292)(2.646)(5.21)
Cap_ESR = 10.02e-6 % (Ohms) % ESD capacitor ESR (11.57e-6)(23.14e-6)
Cable_R = 216e-6 % (Ohms) % Cable resistance per 10 m
Cable_I = 540e-9 % (H) % Cable inductance per 10 m
Final_alpha = 25 % (Deg) % The value of alpha that the bridge ends the last charging cycle on and begins unload - this is used to calculate the unload rate limit

%%--------- CALCULATED PARAMETERS ---------%%
Ir_Lim = (C_V_set)/(Ic_t) % (Volts/second) % Initial charge firing angle rate limit (used to limit main bus transients)
Ir_Lim_neg = (Ir_Lim)*-1 % (Volts/second) % Initial charge firing angle rate negative limit (used to limit main bus transients)
C_V_as_c = (C_V_as)*1.009 % (Volts) % Capacitor voltage after first shot (set by how much residual charge is required between shots to limit MT30 GTA transients (1.01)
T_btw_Lim = ((C_V_set)-(C_V_as_c))/(T_btw_s) % (Volts/second) % Charge between shots firing angle rate limit (used to limit main bus transients)
T_btw_Lim_neg = (T_btw_Lim)*-1 % (Volts/second) % Charge between shots firing angle rate negative limit (used to limit main bus transients)
S_1_t = (Ss_t)+(Ic_t) % (Seconds) % Time of first shot
S_2_t = (S_1_t)+(T_btw_s) % (Seconds) % Time of second shot
S_3_t = (S_2_t)+(T_btw_s) % (Seconds) % Time of third shot
S_4_t = (S_3_t)+(T_btw_s) % (Seconds) % Time of fourth shot
S_5_t = (S_4_t)+(T_btw_s) % (Seconds) % Time of fifth shot
S_6_t = (S_5_t)+(T_btw_s) % (Seconds) % Time of sixth shot
UL_t = S_3_t % (Seconds) % Time of unload
Block = (S_3_t)+(Unl_t) % (Seconds) % Block firing pulses
Unl_Lim = (90-(Final_alpha))/(Unl_t) % (Degrees/second) % Unload firing angle rate limit (used to limit main bus transients)
Unl_Lim_neg = (Unl_Lim)*-1 % (Degrees/second) % Unload firing angle rate negative limit (used to limit main bus transients)

%% Run model
MT30_EM_Railgun_Ver_2;
Appendix J  GTA frequency and voltage deviation against 160 MJ ESD charge time

The following appendix presents the full results of the single shot ESD charge time investigation described in section 5.3.2. The results of the investigation are tabulated in Table J-3 while the frequency and voltage deviation plots for each charge time are shown in Figure J-22 to Figure J-29. The ESD voltage corresponding to each ESD charge time is also shown in Figure J-30 to Figure J-33.

Table J-3 GTA frequency and voltage deviation against 160 MJ ESD charge time

<table>
<thead>
<tr>
<th>Charge time (s)</th>
<th>Minimum frequency (Hz)</th>
<th>Frequency deviation (%)</th>
<th>Minimum voltage (V)</th>
<th>Voltage deviation (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7</td>
<td>50.95</td>
<td>-15.08</td>
<td>8276</td>
<td>-24.76</td>
</tr>
<tr>
<td>7.25</td>
<td>53.57</td>
<td>-10.72</td>
<td>8396</td>
<td>-23.67</td>
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<tr>
<td>7.50</td>
<td>55.13</td>
<td>-8.12</td>
<td>8508</td>
<td>-22.65</td>
</tr>
<tr>
<td>7.75</td>
<td>56.04</td>
<td>-6.60</td>
<td>8613</td>
<td>-21.70</td>
</tr>
<tr>
<td>8</td>
<td>56.78</td>
<td>-5.37</td>
<td>8709</td>
<td>-20.83</td>
</tr>
<tr>
<td>8.25</td>
<td>57.38</td>
<td>-4.37</td>
<td>8798</td>
<td>-20.02</td>
</tr>
<tr>
<td>8.50</td>
<td>57.87</td>
<td>-3.55</td>
<td>8883</td>
<td>-19.25</td>
</tr>
<tr>
<td>8.75</td>
<td>58.24</td>
<td>-2.93</td>
<td>8960</td>
<td>-18.55</td>
</tr>
<tr>
<td>9</td>
<td>58.54</td>
<td>-2.43</td>
<td>9033</td>
<td>-17.88</td>
</tr>
<tr>
<td>9.25</td>
<td>58.78</td>
<td>-2.03</td>
<td>9101</td>
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<tr>
<td>9.50</td>
<td>58.98</td>
<td>-1.70</td>
<td>9166</td>
<td>-16.67</td>
</tr>
<tr>
<td>9.75</td>
<td>59.14</td>
<td>-1.43</td>
<td>9225</td>
<td>-16.14</td>
</tr>
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<td>10</td>
<td>59.25</td>
<td>-1.25</td>
<td>9282</td>
<td>-15.62</td>
</tr>
</tbody>
</table>
GTA frequency and voltage deviation against 160 MJ ESD charge time

**Figure J-22 GTA frequency against 160 MJ ESD charge time – 7 s to 7.75 s**

Figure J-22 shows the GTA frequency plotted against time for 160 MJ ESD charge times of 7 s, 7.25 s, 7.50 s and 7.75 s.

**Figure J-23 GTA frequency against 160 MJ ESD charge time – 8 s to 8.75 s**

Figure J-23 shows the GTA frequency plotted against time for 160 MJ ESD charge times of 8 s, 8.25 s, 8.50 s and 8.75 s.
Figure J-24 GTA frequency against 160 MJ ESD charge time – 9 s to 9.75 s

Figure J-24 shows the GTA frequency plotted against time for 160 MJ ESD charge times of 9 s, 9.25 s, 9.50 s and 9.75 s.

Figure J-25 GTA frequency against 160 MJ ESD charge time – 10 s

Figure J-25 shows the GTA frequency plotted against time for a 160 MJ ESD charge time of 10 s.
Figure J-26 GTA voltage against 160 MJ ESD charge time – 7 s to 7.75 s

Figure J-26 shows the GTA voltage plotted against time for 160 MJ ESD charge times of 7 s, 7.25 s, 7.50 s and 7.75 s.

Figure J-27 GTA voltage against 160 MJ ESD charge time – 8 s to 8.75 s

Figure J-27 shows the GTA voltage plotted against time for 160 MJ ESD charge times of 8 s, 8.25 s, 8.50 s and 8.75 s.
Figure J-28 GTA voltage against 160 MJ ESD charge time – 9 s to 9.75 s

Figure J-28 shows the GTA voltage plotted against time for 160 MJ ESD charge times of 9 s, 9.25 s, 9.50 s and 9.75 s.

Figure J-29 GTA voltage against 160 MJ ESD charge time – 10 s

Figure J-29 shows the GTA voltage plotted against time for a 160 MJ ESD charge time of 10 s.
Figure J-30 ESD charge time preliminary results – 7 s to 7.75 s

Figure J-30 shows the attained ESD capacitor voltage and the set point ESD capacitor voltage plotted against time for charge times of 7 s, 7.25 s, 7.50 s and 7.75 s.

Figure J-31 ESD charge time preliminary results – 8 s to 8.75 s

Figure J-31 shows the attained ESD capacitor voltage and the set point ESD capacitor voltage plotted against time for charge times of 8 s, 8.25 s, 8.50 s and 8.75 s.
Figure J-32 ESD charge time preliminary results – 9 s to 9.75 s

Figure J-32 shows the attained ESD capacitor voltage and the set point ESD capacitor voltage plotted against time for charge times of 9 s, 9.25 s, 9.50 s and 9.75 s.

Figure J-33 ESD charge time preliminary results – 10 s

Figure J-33 shows the attained ESD capacitor voltage and the set point ESD capacitor voltage plotted against time for a charge time of 10 s.
Appendix K  GTA frequency and voltage deviation against GTA unload ramp time

The following appendix presents the full results of the single shot GTA unload time investigation described in section 5.3.2. The results of the investigation are tabulated in Table K-4 while the frequency and voltage deviation plots for each unload time are shown in Figure K-34 to Figure K-43.

Table K-4 GTA frequency and voltage deviation against GTA unload ramp time

<table>
<thead>
<tr>
<th>Unload time (s)</th>
<th>Maximum frequency (Hz)</th>
<th>Frequency deviation (%)</th>
<th>Maximum voltage (V)</th>
<th>Voltage deviation (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2</td>
<td>63.73</td>
<td>6.22</td>
<td>11170</td>
<td>1.55</td>
</tr>
<tr>
<td>2.5</td>
<td>62.83</td>
<td>4.72</td>
<td>11140</td>
<td>1.27</td>
</tr>
<tr>
<td>3</td>
<td>62.42</td>
<td>4.03</td>
<td>11120</td>
<td>1.09</td>
</tr>
<tr>
<td>3.5</td>
<td>62.12</td>
<td>3.53</td>
<td>11100</td>
<td>0.91</td>
</tr>
<tr>
<td>4</td>
<td>61.91</td>
<td>3.18</td>
<td>11090</td>
<td>0.82</td>
</tr>
<tr>
<td>4.5</td>
<td>61.74</td>
<td>2.90</td>
<td>11080</td>
<td>0.73</td>
</tr>
<tr>
<td>5</td>
<td>61.60</td>
<td>2.67</td>
<td>11070</td>
<td>0.64</td>
</tr>
<tr>
<td>5.5</td>
<td>61.48</td>
<td>2.47</td>
<td>11070</td>
<td>0.64</td>
</tr>
<tr>
<td>6</td>
<td>61.38</td>
<td>2.30</td>
<td>11060</td>
<td>0.55</td>
</tr>
<tr>
<td>6.5</td>
<td>61.92</td>
<td>2.15</td>
<td>11050</td>
<td>0.45</td>
</tr>
<tr>
<td>7</td>
<td>61.21</td>
<td>2.02</td>
<td>11050</td>
<td>0.45</td>
</tr>
<tr>
<td>7.5</td>
<td>61.14</td>
<td>1.90</td>
<td>11050</td>
<td>0.45</td>
</tr>
<tr>
<td>8</td>
<td>61.08</td>
<td>1.80</td>
<td>11040</td>
<td>0.36</td>
</tr>
<tr>
<td>8.5</td>
<td>61.02</td>
<td>1.70</td>
<td>11040</td>
<td>0.36</td>
</tr>
<tr>
<td>9</td>
<td>60.97</td>
<td>1.62</td>
<td>11040</td>
<td>0.36</td>
</tr>
<tr>
<td>9.5</td>
<td>60.92</td>
<td>1.53</td>
<td>11040</td>
<td>0.36</td>
</tr>
<tr>
<td>10</td>
<td>60.88</td>
<td>1.47</td>
<td>11040</td>
<td>0.36</td>
</tr>
</tbody>
</table>
Figure K-34 GTA frequency against GTA unload ramp time – 10 s to 8.50 s

Figure K-34 shows the GTA frequency plotted against time for GTA unload ramp times of 10 s, 9.50 s, 9 s and 8.50 s.

Figure K-35 GTA frequency against GTA unload ramp time – 8 s to 6.50 s

Figure K-35 shows the GTA frequency plotted against time for GTA unload ramp times of 8 s, 7.50 s, 7 s and 6.50 s.
Figure K-36 GTA frequency against GTA unload ramp time – 6 s to 4.50 s

Figure K-36 shows the GTA frequency plotted against time for GTA unload ramp times of 6 s, 5.50 s, 5 s and 4.50 s.

Figure K-37 GTA frequency against GTA unload ramp time – 4 s to 2.50 s

Figure K-37 shows the GTA frequency plotted against time for GTA unload ramp times of 4 s, 3.50 s, 3 s and 2.50 s.
Figure K-38 GTA frequency against GTA unload ramp time – 2 s

Figure K-38 shows the GTA frequency plotted against time for a GTA unload ramp time of 2 s.

Figure K-39 GTA voltage against GTA unload ramp time – 10 s to 8.50 s

Figure K-39 shows the GTA voltage plotted against time for GTA unload ramp times of 10 s, 9.50 s, 9 s and 8.50 s.
**GTA frequency and voltage deviation against GTA unload ramp time**

Figure K-40 GTA voltage against GTA unload ramp time – 8 s to 6.50 s

Figure K-40 shows the GTA voltage plotted against time for GTA unload ramp times of 8 s, 7.50 s, 7 s and 6.50 s.

Figure K-41 GTA voltage against GTA unload ramp time – 6 s to 4.50 s

Figure K-41 shows the GTA voltage plotted against time for GTA unload ramp times of 6 s, 5.50 s, 5 s and 4.50 s.
Figure K-42 GTA voltage against GTA unload ramp time – 4 s to 2.50 s

Figure K-42 shows the GTA voltage plotted against time for GTA unload ramp times of 4 s, 3.50 s, 3 s and 2.50 s.

Figure K-43 GTA voltage against GTA unload ramp time – 2 s

Figure K-43 shows the GTA voltage plotted against time for a GTA unload ramp time of 2 s.
Appendix L  Results of instant recharge – 160 MJ ESD

This appendix shows the full results of a test case run to simulate attempting to recharge the ESD in the manner suggested by Chaboki, et al., (2004). As demonstrated in Figure L-44 the immediate reduction of the ESD voltage to near zero following the shot causes the GTA to rapidly shed load, as shown in the first and second plots respectively. This is because the GTA load demand is a function of the capacitor voltage and charging current. Hence, when the voltage reduces to zero the load demand reduces to zero and an unbalance between the generation and load occurs. In attempting the balance the GTA power and the load demand the GTA load shed logic reduces the GTA fuel supply thus drastically reducing its output power, as shown in Figure L-44.

Furthermore and owing to the capacitor inrush current, the GTA RMS voltage dips to 5 kV. This is equivalent to a 55% voltage drop which is outside of NATO STANAG 1008 QPS limits. As the ESD charging bridge is still demanding energy whilst the GTA is shedding load, the energy is extracted from the GTA inertia and the GTA frequency collapses, as shown in the bottom plot. The previously discussed impacts on the power system can be attributed to the ESD capacitor inrush current evoking the sub-transient reactance of the GTA, as described in section 3.3.1. As the AVR cannot mitigate for this impact, other methods to maintain an acceptable level of QPS during continuous firing operations must be sought.
Results of instant recharge – 160 MJ ESD

Figure L-44 ESD voltage, GTA RMS voltage, GTA real power and GTA frequency

Figure L-45 Corresponding firing delay angle
The following appendix presents the full results of the GTA load shed investigation described in section 5.4.2. The results of the investigation are tabulated in Table M-5 while the frequency and voltage deviation plots for each load shed are shown in Figure M-46 to Figure M-53.

<table>
<thead>
<tr>
<th>Load shed (MW)</th>
<th>Maximum frequency (Hz)</th>
<th>Frequency deviation (%)</th>
<th>Maximum voltage (V)</th>
<th>Voltage deviation (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>60.16</td>
<td>0.27</td>
<td>11020</td>
<td>0.18</td>
</tr>
<tr>
<td>2</td>
<td>60.33</td>
<td>0.55</td>
<td>11040</td>
<td>0.36</td>
</tr>
<tr>
<td>3</td>
<td>60.50</td>
<td>0.83</td>
<td>11060</td>
<td>0.55</td>
</tr>
<tr>
<td>4</td>
<td>60.67</td>
<td>1.12</td>
<td>11080</td>
<td>0.73</td>
</tr>
<tr>
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<td>60.84</td>
<td>1.40</td>
<td>11100</td>
<td>0.91</td>
</tr>
<tr>
<td>6</td>
<td>61.02</td>
<td>1.70</td>
<td>11120</td>
<td>1.09</td>
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<td>7</td>
<td>61.19</td>
<td>1.98</td>
<td>11140</td>
<td>1.27</td>
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<td>4.03</td>
<td>11300</td>
<td>2.73</td>
</tr>
</tbody>
</table>
Figure M-46 GTA frequency following load shed – 1 MW to 4 MW

Figure M-46 shows the GTA frequency plotted against time for load sheds of 1 MW to 4 MW.

Figure M-47 GTA frequency following load shed – 5 MW to 8 MW

Figure M-47 shows the GTA frequency plotted against time for load sheds of 5 MW to 8 MW.
GTA frequency and voltage deviation against GTA load shed

Figure M-48 GTA frequency following load shed – 9 MW to 12 MW

Figure M-48 shows the GTA frequency plotted against time for load sheds of 9 MW to 12 MW.

Figure M-49 GTA frequency following load shed – 13 MW

Figure M-49 shows the GTA frequency plotted against time for a load shed of 13 MW.
Figure M-50 GTA voltage following load shed – 1 MW to 4 MW

Figure M-50 shows the GTA voltage plotted against time for load sheds of 1 MW to 4 MW.

Figure M-51 GTA voltage following load shed – 5 MW to 8 MW

Figure M-51 shows the GTA voltage plotted against time for load sheds of 5 MW to 8 MW.
Figure M-52 GTA voltage following load shed – 9 MW to 12 MW

Figure M-52 shows the GTA voltage plotted against time for load sheds of 9 MW to 12 MW.

Figure M-53 GTA voltage following load shed – 13 MW

Figure M-53 shows the GTA voltage plotted against time for a load shed of 13 MW.
Appendix N  Required ESD capacity calculations

To plot the relationship between the capacity of the ESD and the corresponding GTA load shed the following procedure was repeated for an ESD capacity of the minimum required 160 MJ, up to 3.24 GJ, in 5 MJ increments. At 3.24 GJ the permitted load shed was 1 MW, which was considered the minimum practical load shed to consider.

Firstly, considering that

\[ E = \frac{1}{2} CV^2 \]  
\[ \text{[E92]} \]

Then

\[ C = \frac{2E}{V^2} \]  
\[ \text{[E93]} \]

Where \( E \) is the stored energy (J), \( C \) is the capacitance of the capacitor (F) and \( V \) is the capacitor voltage when fully charged (V).

Following this, the residual energy following a shot was calculated which is equal to the overall stored energy minus the 160 MJ required per shot. The capacitor voltage following the shot was then calculated thus

\[ V_{\text{shot}} = \sqrt{\frac{2E_{\text{residual}}}{C}} \]  
\[ \text{[E94]} \]

Where \( C \) is the capacitance of the capacitor (F), \( E_{\text{residual}} \) is the residual energy in the capacitor following the shot (J) and \( V_{\text{shot}} \) is the voltage of the capacitor following the shot (V).
Required ESD capacity calculations

$V_{\text{shot}}$, which is now the load voltage was then calculated as a percentage of the maximum charge voltage thus

$$Load\ voltage\ (\%) = 100 - \left[ \left( \frac{V_{\text{shot}}}{11000} \right) \times 100 \right] \quad [E95]$$

This percentage was then taken in terms of the GTA maximum power, to determine the corresponding load shed as follows

$$\text{Corresponding load shed} = \left( \frac{40}{1100} \right) \times Load\ voltage\ (%)$$

**Figure N-54** Total capacity of ESD required against maximum permitted GTA load shed from 1 MW – 40 MW

**Figure N-55** Total capacity of ESD required against maximum permitted GTA load shed from 5 MW – 39 MW
Required ESD capacity calculations
The described calculation process was completed via a spreadsheet, the results of which are shown in Figure
N-56.

Total ESD Capacitor Capacitor Residual Voltage following
capacity (J) voltage (V) size (F) charge (J)
shot (V)
1.60E+08 1.10E+04 2.64E+00 0.00E+00
0.00E+00
1.65E+08 1.10E+04 2.73E+00 5.00E+06
1.91E+03
1.70E+08 1.10E+04 2.81E+00 1.00E+07
2.67E+03
1.75E+08 1.10E+04 2.89E+00 1.50E+07
3.22E+03
1.80E+08 1.10E+04 2.98E+00 2.00E+07
3.67E+03
1.85E+08 1.10E+04 3.06E+00 2.50E+07
4.04E+03
1.90E+08 1.10E+04 3.14E+00 3.00E+07
4.37E+03
1.95E+08 1.10E+04 3.22E+00 3.50E+07
4.66E+03
2.00E+08 1.10E+04 3.31E+00 4.00E+07
4.92E+03
2.05E+08 1.10E+04 3.39E+00 4.50E+07
5.15E+03
2.10E+08 1.10E+04 3.47E+00 5.00E+07
5.37E+03
2.15E+08 1.10E+04 3.55E+00 5.50E+07
5.56E+03
2.20E+08 1.10E+04 3.64E+00 6.00E+07
5.74E+03
2.25E+08 1.10E+04 3.72E+00 6.50E+07
5.91E+03
2.30E+08 1.10E+04 3.80E+00 7.00E+07
6.07E+03
2.35E+08 1.10E+04 3.88E+00 7.50E+07
6.21E+03
2.40E+08 1.10E+04 3.97E+00 8.00E+07
6.35E+03
2.45E+08 1.10E+04 4.05E+00 8.50E+07
6.48E+03
2.50E+08 1.10E+04 4.13E+00 9.00E+07
6.60E+03
2.55E+08 1.10E+04 4.21E+00 9.50E+07
6.71E+03
2.60E+08 1.10E+04 4.30E+00 1.00E+08
6.82E+03
2.65E+08 1.10E+04 4.38E+00 1.05E+08
6.92E+03
2.70E+08 1.10E+04 4.46E+00 1.10E+08
7.02E+03
2.75E+08 1.10E+04 4.55E+00 1.15E+08
7.11E+03
2.80E+08 1.10E+04 4.63E+00 1.20E+08
7.20E+03
2.85E+08 1.10E+04 4.71E+00 1.25E+08
7.28E+03
2.90E+08 1.10E+04 4.79E+00 1.30E+08
7.36E+03
2.95E+08 1.10E+04 4.88E+00 1.35E+08
7.44E+03
3.00E+08 1.10E+04 4.96E+00 1.40E+08
7.51E+03
3.05E+08 1.10E+04 5.04E+00 1.45E+08
7.58E+03
3.10E+08 1.10E+04 5.12E+00 1.50E+08
7.65E+03
3.15E+08 1.10E+04 5.21E+00 1.55E+08
7.72E+03
3.20E+08 1.10E+04 5.29E+00 1.60E+08
7.78E+03
3.25E+08 1.10E+04 5.37E+00 1.65E+08
7.84E+03
3.30E+08 1.10E+04 5.45E+00 1.70E+08
7.90E+03
3.35E+08 1.10E+04 5.54E+00 1.75E+08
7.95E+03
3.40E+08 1.10E+04 5.62E+00 1.80E+08
8.00E+03
3.45E+08 1.10E+04 5.70E+00 1.85E+08
8.06E+03
3.50E+08 1.10E+04 5.79E+00 1.90E+08
8.10E+03
3.55E+08 1.10E+04 5.87E+00 1.95E+08
8.15E+03
3.60E+08 1.10E+04 5.95E+00 2.00E+08
8.20E+03
3.65E+08 1.10E+04 6.03E+00 2.05E+08
8.24E+03
3.70E+08 1.10E+04 6.12E+00 2.10E+08
8.29E+03
3.75E+08 1.10E+04 6.20E+00 2.15E+08
8.33E+03
3.80E+08 1.10E+04 6.28E+00 2.20E+08
8.37E+03
3.85E+08 1.10E+04 6.36E+00 2.25E+08
8.41E+03
3.90E+08 1.10E+04 6.45E+00 2.30E+08
8.45E+03
3.95E+08 1.10E+04 6.53E+00 2.35E+08
8.48E+03
4.00E+08 1.10E+04 6.61E+00 2.40E+08
8.52E+03
4.05E+08 1.10E+04 6.69E+00 2.45E+08
8.56E+03
4.10E+08 1.10E+04 6.78E+00 2.50E+08
8.59E+03
4.15E+08 1.10E+04 6.86E+00 2.55E+08
8.62E+03
4.20E+08 1.10E+04 6.94E+00 2.60E+08
8.65E+03
4.25E+08 1.10E+04 7.02E+00 2.65E+08
8.69E+03
4.30E+08 1.10E+04 7.11E+00 2.70E+08
8.72E+03
4.35E+08 1.10E+04 7.19E+00 2.75E+08
8.75E+03
4.40E+08 1.10E+04 7.27E+00 2.80E+08
8.77E+03
4.45E+08 1.10E+04 7.36E+00 2.85E+08
8.80E+03
4.50E+08 1.10E+04 7.44E+00 2.90E+08
8.83E+03
4.55E+08 1.10E+04 7.52E+00 2.95E+08
8.86E+03
4.60E+08 1.10E+04 7.60E+00 3.00E+08
8.88E+03
4.65E+08 1.10E+04 7.69E+00 3.05E+08
8.91E+03
4.70E+08 1.10E+04 7.77E+00 3.10E+08
8.93E+03
4.75E+08 1.10E+04 7.85E+00 3.15E+08
8.96E+03
4.80E+08 1.10E+04 7.93E+00 3.20E+08
8.98E+03
4.85E+08 1.10E+04 8.02E+00 3.25E+08
9.00E+03
4.90E+08 1.10E+04 8.10E+00 3.30E+08
9.03E+03
4.95E+08 1.10E+04 8.18E+00 3.35E+08
9.05E+03
5.00E+08 1.10E+04 8.26E+00 3.40E+08
9.07E+03

% of total
1.00E+02
8.26E+01
7.57E+01
7.07E+01
6.67E+01
6.32E+01
6.03E+01
5.76E+01
5.53E+01
5.31E+01
5.12E+01
4.94E+01
4.78E+01
4.63E+01
4.48E+01
4.35E+01
4.23E+01
4.11E+01
4.00E+01
3.90E+01
3.80E+01
3.71E+01
3.62E+01
3.53E+01
3.45E+01
3.38E+01
3.30E+01
3.24E+01
3.17E+01
3.11E+01
3.04E+01
2.99E+01
2.93E+01
2.87E+01
2.82E+01
2.77E+01
2.72E+01
2.68E+01
2.63E+01
2.59E+01
2.55E+01
2.51E+01
2.47E+01
2.43E+01
2.39E+01
2.36E+01
2.32E+01
2.29E+01
2.25E+01
2.22E+01
2.19E+01
2.16E+01
2.13E+01
2.10E+01
2.08E+01
2.05E+01
2.02E+01
2.00E+01
1.97E+01
1.95E+01
1.92E+01
1.90E+01
1.88E+01
1.86E+01
1.84E+01
1.81E+01
1.79E+01
1.77E+01
1.75E+01

Permitted load
Capacitor based ESD Capacitor based ESD mass
step
volume (m3) (1 MJ/M^3)
(kg) (1230 kg / m^3)
4.00E+01
160.00
196.80
3.30E+01
165.00
202.95
3.03E+01
170.00
209.10
2.83E+01
175.00
215.25
2.67E+01
180.00
221.40
2.53E+01
185.00
227.55
2.41E+01
190.00
233.70
2.31E+01
195.00
239.85
2.21E+01
200.00
246.00
2.13E+01
205.00
252.15
2.05E+01
210.00
258.30
1.98E+01
215.00
264.45
1.91E+01
220.00
270.60
1.85E+01
225.00
276.75
1.79E+01
230.00
282.90
1.74E+01
235.00
289.05
1.69E+01
240.00
295.20
1.64E+01
245.00
301.35
1.60E+01
250.00
307.50
1.56E+01
255.00
313.65
1.52E+01
260.00
319.80
1.48E+01
265.00
325.95
1.45E+01
270.00
332.10
1.41E+01
275.00
338.25
1.38E+01
280.00
344.40
1.35E+01
285.00
350.55
1.32E+01
290.00
356.70
1.29E+01
295.00
362.85
1.27E+01
300.00
369.00
1.24E+01
305.00
375.15
1.22E+01
310.00
381.30
1.19E+01
315.00
387.45
1.17E+01
320.00
393.60
1.15E+01
325.00
399.75
1.13E+01
330.00
405.90
1.11E+01
335.00
412.05
1.09E+01
340.00
418.20
1.07E+01
345.00
424.35
1.05E+01
350.00
430.50
1.04E+01
355.00
436.65
1.02E+01
360.00
442.80
1.00E+01
365.00
448.95
9.87E+00
370.00
455.10
9.71E+00
375.00
461.25
9.56E+00
380.00
467.40
9.42E+00
385.00
473.55
9.28E+00
390.00
479.70
9.15E+00
395.00
485.85
9.02E+00
400.00
492.00
8.89E+00
405.00
498.15
8.77E+00
410.00
504.30
8.65E+00
415.00
510.45
8.53E+00
420.00
516.60
8.41E+00
425.00
522.75
8.30E+00
430.00
528.90
8.20E+00
435.00
535.05
8.09E+00
440.00
541.20
7.99E+00
445.00
547.35
7.89E+00
450.00
553.50
7.79E+00
455.00
559.65
7.70E+00
460.00
565.80
7.60E+00
465.00
571.95
7.51E+00
470.00
578.10
7.43E+00
475.00
584.25
7.34E+00
480.00
590.40
7.26E+00
485.00
596.55
7.17E+00
490.00
602.70
7.09E+00
495.00
608.85
7.02E+00
500.00
615.00

Figure N-56 Required ESD capacity calculation spreadsheet results

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Appendix O  Single shot and continuous firing with HV 5th harmonic filter

This appendix presents the results obtained to support the discussion presented in section 6.3. The performance of the power system under reduced harmonic waveform distortion was assessed by upgrading the performance tool described in Chapter 4 to include the 5th harmonic filter designed in section 6.3. The resulting performance tool system diagram is shown in Figure 6-25. The single shot and continuous EM railgun firing investigations described in section 5.3.3 and section 5.4.3 in Chapter 5 were then repeated with the performance tool shown in Figure 6-25. The filter parameters were as per Table 6-8. The full results of this investigation are presented here. The performance tool model top level schematic with the harmonic filter is shown in Figure O-57.

Figure O-57 Performance tool top level Simulink® model schematic with 5th harmonic filter
Single shot case study with 5th harmonic filter

The following section presents the results of the single shot case study as described in section 5.3.3 with the 5th harmonic filter included.

![GTA RMS current](image_url)

![GTA RMS voltage](image_url)

![GTA frequency](image_url)

**Figure O-58 GTA RMS current, RMS voltage and frequency during single shot operation with 5th harmonic filter**

Figure O-58 shows the GTA RMS current, RMS voltage and GTA frequency on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure O-58 shows that upon commencement of ESD charging there is an immediate demand for charging current which rises immediately from 250 A to 2 kA. The GTA RMS voltage dips to 9.75 kV (-11.36%) then stabilises at 11 kV throughout the charging phase. The GTA frequency dips to 58.5 Hz (-2.50%) which is within the tolerance as defined by NATO STANAG 1008. The GTA frequency then recovers and remains at 59 Hz throughout the charging period.

At the point at which the shot is fired Figure O-58 shows the GTA RMS voltage rising to 11.50 kV (4.54%) and the GTA frequency increasing to 61.50 Hz (2.50%). Both of these transients are within tolerance as defined by NATO STANAG 1008. When the GTA unload is complete the GTA RMS voltage dips to 10.75 kV (-2.27%) which is within tolerance as defined by NATO STANAG 1008. As the voltage and frequency deviations remain within tolerance during the GTA unload the recovery time can be negated.
Single shot and continuous firing with HV 5th harmonic filter

Figure O-59 GTA apparent, real and reactive power during single shot operation with 5th harmonic filter

Figure O-59 shows the GTA apparent power, real power and reactive power on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure O-59 demonstrates that upon commencement of ESD charging the GTA apparent power increases to 35 MVA then increases approximately linearly to 45 MVA throughout the ESD charging period. The GTA real power exhibits a linear increase during the charging period increasing from the 2 MW EM railgun support load to 40 MW, which represents a temporary 110% overload of the GTA. Point A on Figure O-59 shows a spike of real power. This is an anomalous result associated with the initialisation of the harmonic filter within the simulation. This spike does not manifest in the GTA RMS current plot shown in Figure O-58. The GTA reactive power increases to 35 MVAr then decreases approximately linearly to 20 MVAr throughout the ESD charging period.

During the GTA unload the GTA apparent, real and reactive power all decrease back to match the 2 MW EM railgun support load. Point B in Figure O-59 shows the GTA reactive power rising towards the end of the GTA unload before the six-pulse thyristor rectifier is disconnected from the main bus. This is because as the GTA unloads into the brake resistor, the rate of which is controlled by the six-pulse thyristor rectifier, the reactive power absorbed by the harmonic source decreases. Once the reactive power absorbed by the six-pulse rectifier is less than 17.5 MVAr the 5th harmonic filter begins to dominate because it is generating more reactive power than is being absorbed by the six-pulse rectifier. As such, the reactive power at the main bus begins to increase as the real power continues to decrease. At this point the GTA is importing MVAr as opposed to exporting MVArs under lagging power factor conditions. This is because the harmonic filter is generating more reactive power than is being absorbed by the six-pulse thyristor rectifier.
Figure O-60 Charging bridge firing delay angle, GTA power factor and main bus THD
during single shot operation with 5th harmonic filter

Figure O-60 shows the charging bridge firing delay angle, the GTA power factor and the main bus THD on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure O-60 shows that during the ESD charging stage the charging bridge firing delay angle decreases from 90 degrees to 35 degrees within the 8.75 s charge time before increasing from 35 degrees to 90 degrees within the 4.50 s GTA unload time. The GTA power factor decreases from 0.75 lagging to 0.15 lagging immediately as charging commences, increasing approximately linearly throughout the ESD charging cycle to 0.90 lagging. The power factor then decreases to 0.40 leading during the GTA unload before returning to 0.75 lagging once the GTA unload is complete.

The main bus THD increases immediately from 2.50% to approximately 8%, before decreasing linearly to approximately 6% throughout the ESD charging cycle. As the GTA unloads the THD decreases to less than 5% once the unload is complete. It is acknowledged that the THD plot presented in Figure O-60 is oscillating and that an accurate THD reading is difficult to ascertain. To address this, the THD at fixed points was calculated by means of a Fast Fourier Transform and is presented in section 6.3.
Single shot and continuous firing with HV 5th harmonic filter

Figure O-61 5th harmonic filter branch voltages and currents during single shot EM railgun firing

Figure O-61 shows the branch voltages and currents for the 5th harmonic filter designed in section 6.3. The section of waveform analysed relates to the RMS voltage waveform shown in Figure O-58. As shown in Figure O-61 the maximum peak filter branch voltage is approximately 15 kV and the maximum peak branch current is approximately 1.25 kA. No peak voltages or currents were observed that are outside the ratings of the warship electric power system, thus the filter design is considered acceptable from a voltage and current rating perspective.
Continuous firing case study with 5th harmonic filter

Figure O-62 GTA RMS current, RMS voltage and frequency during continuous firing operation – 305 MJ ESD with 5th harmonic filter

Figure O-62 shows the GTA RMS current, RMS voltage and GTA frequency on the y axis of the upper, middle and lower plot respectively, all plotted against time on the x axis. Figure O-62 shows that upon commencement of ESD charging there is an immediate demand for charging current which rises immediately from 250 A to 2 kA. The GTA RMS voltage dips to 9.75 kV (-11.36%) then stabilises at 11 kV throughout the ESD charging phase. The GTA frequency dips to 59.50 Hz (-0.83%) which is within tolerance as defined by NATO STANAG 1008. The GTA frequency remains at 59.50 Hz throughout the ESD charging period.

Figure O-62 demonstrates that during continuous firing when employing a 305 MJ ESD and a 5th harmonic filter the system is dynamically stable. This means that the voltage and frequency transients resulting from each EM railgun shot and ESD recharge are repeatable and the system is not exhibiting any signs of degradation. This provides confidence that this rate of fire could be maintained. As shown in Figure O-62 the system reaches dynamic stability by the fourth shot at 36.50 s. The corresponding voltage and frequency deviation values are discussed further in section 6.3 and are shown to be within acceptable QPS limits as defined by NATO STANAG 1008 throughout the continuous firing EM railgun operation.
Single shot and continuous firing with HV 5th harmonic filter

Figure O-63 GTA apparent, real and reactive power during continuous firing operation – 305 MJ ESD with 5th harmonic filter

Figure O-63 shows the GTA apparent power, real power and reactive power on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure O-63 demonstrates that upon commencement of ESD charging the GTA apparent power increases to 35 MVA then increases approximately linearly to 45 MVA throughout the ESD charging period. The GTA real power exhibits a linear increase during the ESD charging period increasing from the 2 MW EM railgun support load to 40 MW, which represents a temporary 110% overload of the GTA. Point A on Figure O-63 shows a spike of real power. This is an anomalous result associated with the initialisation of the harmonic filter within the simulation. This spike does not manifest in the GTA current plot shown in Figure O-62. The GTA reactive power increases to 35 MVAr then decreases approximately linearly to 20 MVAr throughout the ESD charging period.

At the point of ESD recharge the capacitor inrush current can clearly be seen manifesting in each of the three power plots as short lived spikes of power. The GTA takes a 10 MW load shed following each shot which corresponds to the discharging of 160 MJ from the ESD. During the recharge period the GTA apparent power remains at approximately 40 MVA, while the GTA real power ramps linearly back to 40 MW following the 10 MW load shed. The GTA reactive power decreases linearly throughout each recharging period from 30 MVAr to 20 MVAr. Following the sixth shot at 46.50 s the GTA unloads back to match the 2 MW EM railgun support load. Point B in Figure O-63 shows the GTA reactive power rising towards the end of the GTA unload before the six-pulse thyristor rectifier is disconnected from the main bus. The explanation for this is as per that given when explaining point B in Figure O-59.
Single shot and continuous firing with HV 5th harmonic filter

Figure O-64 Charging bridge firing delay angle, GTA power factor and main bus THD during continuous firing operation – 305 MJ ESD with 5th harmonic filter

Figure O-64 shows the charging bridge firing delay angle, the GTA power factor and the main bus THD on the y axis of the upper, middle and lower plots respectively, all plotted against time on the x axis. Figure O-64 shows that during the ESD charging period the charging bridge firing delay angle decreases approximately linearly from 90 degrees to 35 degrees within the 16.50 s ESD charge time. The GTA power factor decreases from 0.75 lagging to 0.15 lagging immediately as ESD charging commences. The GTA power factor then increases approximately linearly from 0.15 lagging to 0.90 lagging throughout the ESD charging cycle, as the firing delay angle decreases. During each ESD recharge the GTA power factor decreases to 0.70 lagging before recovering approximately linearly to 0.90 lagging as the ESD is recharged.

During the GTA unload the firing delay angle increases from 35 degrees to 90 degrees within the 4.50 s GTA unload time. The GTA power factor decreases to 0.35 leading before returning immediately to 0.75 lagging once the GTA unload is complete. It is acknowledged that the THD plot presented in Figure O-64 is oscillating and that an accurate THD reading is difficult to ascertain. To address this, the THD at fixed points was calculated by means of a Fast Fourier Transform and is presented in section 6.3.